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ORBIT TRANSFER VEHICLE
ENGINE STUDY, PHASE A - CONTINUATION
STUDY RESULTS

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ORBIT TRANSFER VEHICLE ENGINE STUDY, PHASE A - CONTINUATION

(Study Results)

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FOREWORD

This report presents the results of studies conducted during the continuation period of the Orbital Transfer Vehicle Engine Study, Phase A - Contract. Engine programmatics at low pump NPSH, engine operation at intermediate thrust levels and low thrust engine operation during aerobraking maneuvers were investigated by Rocketdyne, a Division of Rockwell International, under Contract NAS8-32996--administered by Marshall Space Flight Center (MSFC) of the National Aeronautics and Space Administration (NASA). The NASA Contracting Officer's Representative was Mr. Fred Braam of MSFC. Mr. H. G. Diem was the Rocketdyne Program Manager. The technical effort was conducted under the direction of Mr. A. Martinez, Study Manager.

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ACKNOWLEDGEMENTS

Contributions to this report were made by the following personnel:

Engine Steady-State Performance Analysis	-	J. Glass
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Programmatics	-	C. Meisl
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INTRODUCTION AND SUMMARY

The Orbital Transfer Vehicle (OTV) in which the oxygen/hydrogen OTV engine will be utilized is part of the planned space transportation system (STS). The OTV, carried into a low earth orbit by the Space Shuttle Orbiter, will extend the operating regime of the STS beyond the capability of the basic Space Shuttle, to include higher altitude orbits, geosynchronous orbits, and other space missions. For the past several years. NASA, the Department of Defense, and various vehicle and propulsion system contractors, including Rocketdyne, have been studying OTV related requirements, missions, vehicle configurations, and engine systems for this application. The goals of the vehicle and propulsion system studies have been the definition of and technology development for a high-performance, low-operating cost, reusable long life space system with the required operational versatility and payload retrieval capability.

Three concepts have been defined by NASA and vehicle contractors as potential designs for the Orbital Transfer Vehicle: these are the all-propulsive orbit transfer vehicle (APOTV), the aero-maneuvering orbit transfer vehicle (AMOTV), and the aerobraking orbit transfer vehicle (ABOTV). The baseline design of these preliminary studies had assumed an uprated shuttle orbiter with 100,000 pound payload capability to low earth orbit. As an interim vehicle, the current 65,000-pound payload capability shuttle orbiter would be used.

The baseline mission defined by NASA for the OTV Phase A Engine Studies was a four-man, 30-day sortie from low earth orbit to geosynchronous orbit and return to low earth orbit. A reusable oxygen-hydrogen upper stage with the high performance which these propellants offer is essential to achieving a defined round trip payload of 13,000 pounds.

The OTV Engine Phase A Continuation studies performed by Rocketdyne have resulted in:

(1) Selection of boost pump designs for low NPSH operation and generation of associated programmatic data.

(2) Evaluation of OTV engine operation at intermediate thrust levels and impact on programmatics.

(3) Assessment of OTV engine operation at idle-mode thrusts under conditions experienced during aerobraking maneuvers of the ABOTV.

As a result of the studies conducted, it is recommended that the original OTV boost pump designs be used without change for low NPSH operation. Lower programmatic impact will be experienced without loss in suction performance capability.

Intermediate thrust operation is feasible for both the expander cycle and staged combustion cycles. The engines can be adapted for intermediate thrust operation through minor modifications of the preburner, main injector, pumps, dump coolant control valve and certain component connecting ducts.

Operating conditions defined during aerobraking maneuvers do not impose any problems in engine operation at pumped idle mode. However, in tank-head idle mode, nozzle flow separation and side-loads for the bipropellant mode and flow instability for the hydrogen-only mode are predicted. Operation of the engine under these conditions is not recommended.

To accomplish the effort described in the Statement of Work (SOW), the following technical study tasks were added to the original contract.

Task 13 Programmatics for Low NPSH Operation
Task 15 Intermediate Thrust Operation
Task 16 Engine Operation for an Aerobraking OTV (ABOTV)

This report is organized by study tasks and summarizes the results of the three tasks defined above. The work was performed during a five-month period, from October 1980 to February 1981.

TASK 13. PROGRAMMATICS FOR LOW NPSH OPERATION

The intermediate objective of this task was to conduct an analysis of engine operation at low tank NPSH values of sufficient depth to define the impact on engine design. Tank NPSH conditions examined and stipulated in the SOW are as indicated in Table 1. The ultimate objective of the study was to define by WBS element the impact on previously reported DDT&E, production, and operations costs and schedules of design changes required for the baseline engine. Baseline design tank NPSH values for the OTV engine pumps were 2 and 15 feet, respectively, for the oxygen and hydrogen pumps. Low NPSH is desired because it reduces the vehicle propellant tank pressurization system requirements and therefore reduces system weights. It has been shown in vehicle studies (Ref. 1) that pressurization system weights can be reduced approximately 60 lb for OTV-type missions by reducing NPSH in the hydrogen boost pump to 4 feet and the oxygen pump to 1.5 feet from the baseline values of 15 and 2 feet, respectively. Most of the weight savings are attributed to the hydrogen pump. Additional weight savings would occur if the hydrogen pump could be operated at zero tank NPSH with various amounts of vapor in the flow.

TABLE 1. LOW NPSH OPERATING CONDITIONS, OTV ENGINES

PUMPED FLUID	TANK NPSH, FT	VAPOR FRACTION, % VOL
Oxygen	2	-
Hydrogen	15	-
Hydrogen	10	-
Hydrogen	5	-
Hydrogen	0	5
Hydrogen	0	10
Hydrogen	0	20

It was confirmed in this study that finite NPSH OTV boost pump designs can operate at zero NPSH as efficiently as boost pump designs generated specifically for zero NPSH operation. It was also determined that finite NPSH fuel pump designs in the range 0-10 Ft would have the following disadvantages compared to the selected 15 Ft design; (1) larger inlet diameter and larger weight, (2) lower inlet flow coefficient with associated suction performance losses, and (3) increased suction performance limitations at off-design pumped-idle thrust. As indicated in Appendix A the baseline 15 Ft NPSH hydrogen pump and the 2 Ft NPSH oxygen pump can operate under "zero" tank NPSH conditions and various amounts of vapor at the pump inlet. These designs are thus recommended without major change for "zero" NPSH tank conditions. Though no impact on engine design was established a slight impact on cost was found. The impact had to do with the added requirements to the engine of operating in low NPSH conditions and the more meticulous pump analysis, design, and testing required to verify pump operation at NPSH values below those originally designed for.

IMPACT ON ENGINE DESIGN

The impact on engine design of low NPSH operation occurs chiefly through the turbomachinery design. Two types of pump designs were examined: one where a finite low NPSH exists at the tank and vapor-free flow is required at the pump inlet, and another where zero tank NPSH exists and vapor is allowed at the pump inlets.

Finite NPSH with Vapor-Free Flow Designs

The inlet line conditions for pump designs with finite tank NPSH and zero vapor at the pump inlet are indicated in Figure 1 for hydrogen and oxygen. As tank NPSH decreases, fluid velocity must decrease to maintain saturated liquid conditions at the pump inlet; thus inlet diameter increases. As indicated in the figure, NPSH values near zero with no vapor in the pump inlet flow requires relatively large pump inlet diameters. Higher NPSH values result in more reasonable pump inlet sizes. Depending on the engine inlet line losses, indicated in Figure 1 in velocity heads, the inlet pump diameter for the 15-foot

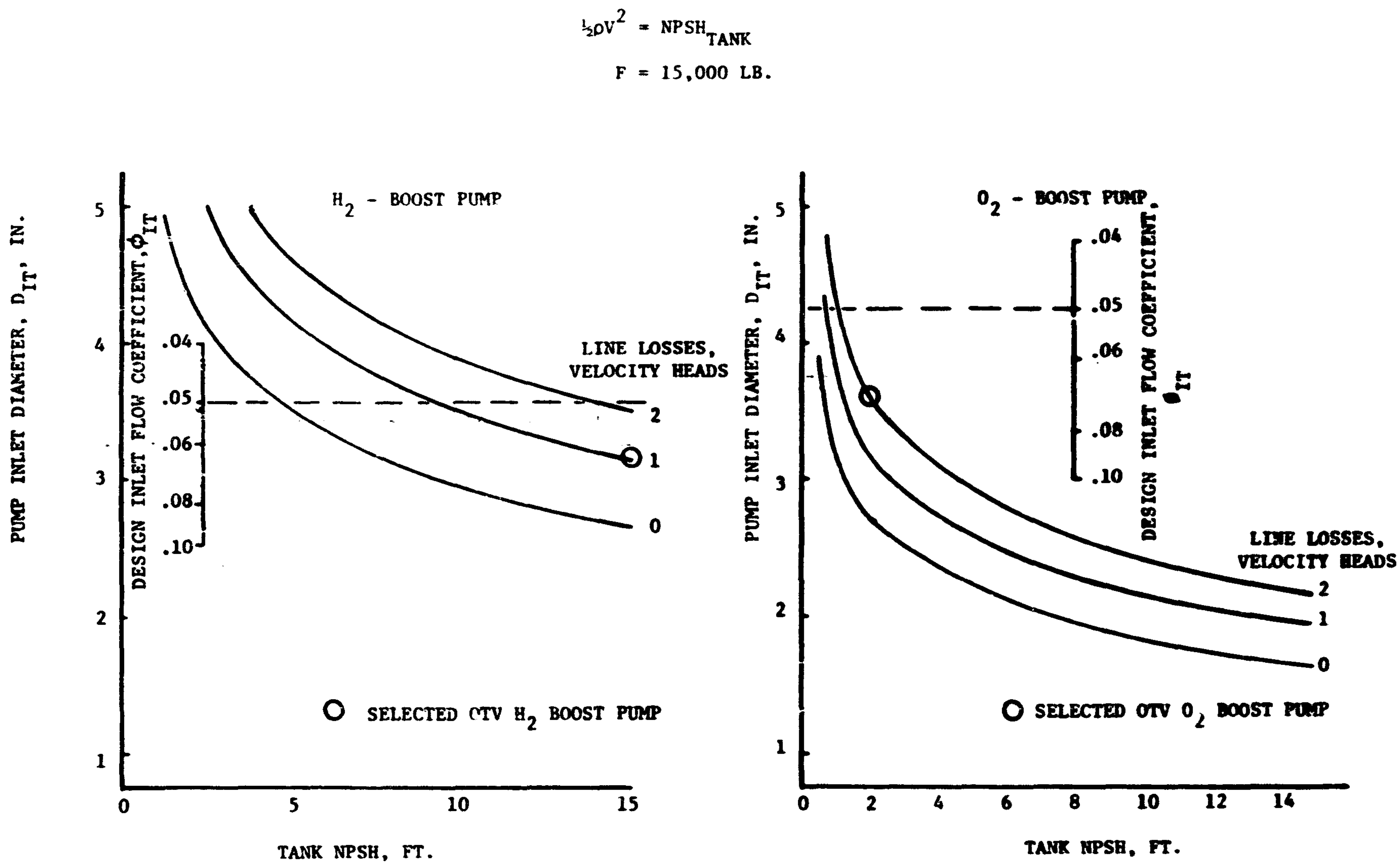


Figure 1. OTV BOOST PUMP INLET DIAMETER REQUIREMENTS FOR SATURATED LIQUID AT THE PUMP INLET

NPSH case could vary between 2.65 to 3.5 inches. A diameter of 3.14 inches was selected for the 15K engine hydrogen boost pump to allow for typical vehicle inlet line losses. For the same reason, a diameter of 3.6 inches was selected for the oxygen boost pump at 2 feet NPSH. These selections also satisfy minimum inlet flow coefficient limits of 0.05 below which design point pump suction performance is significantly affected (Fig. 1) due to blade blockage at lower values.

Zero Tank NPSH with Vapor-In-Inlet-Flow Designs

Inlet line diameters for pump designs with saturated liquid in the tank and vapor in the pump inlet flow are shown in Fig. 2 for the hydrogen boost pump compared to the finite NPSH pump design cases. Since vapor is allowed in the flow, pump inlet diameters decrease as the allowable vapor fraction is increased. A vapor fraction of 30 percent by volume is considered the design limit.

An isentropic flow expansion has been assumed to the allowable vapor fraction. The change in enthalpy during isentropic expansion is larger for the higher vapor fractions; thus flow velocities are higher and pump inlet diameters can be lower for the same design mass flowrate. As shown in Fig. 2, zero vapor conditions again require very large diameters. Increasing line losses also require larger pump inlet diameters since the total expansion pressure differential is not all available for flow velocity increase, and a portion is lost to friction.

As was the case with the finite NPSH pump design, the requirement for a lower limit of 0.05 on the design inlet flow coefficient precludes designs above the indicated dashed line. According to the above analysis, the baseline 15K OTV hydrogen boost pump inlet diameter of 3.14 inches supplies the pump diffuser with 10 percent vapor fraction at full thrust. A more detailed analysis for the same pump with a particular OTV vehicle propellant duct configuration and vehicle accelerations (shown in Appendix A) indicated the inlet line would supply

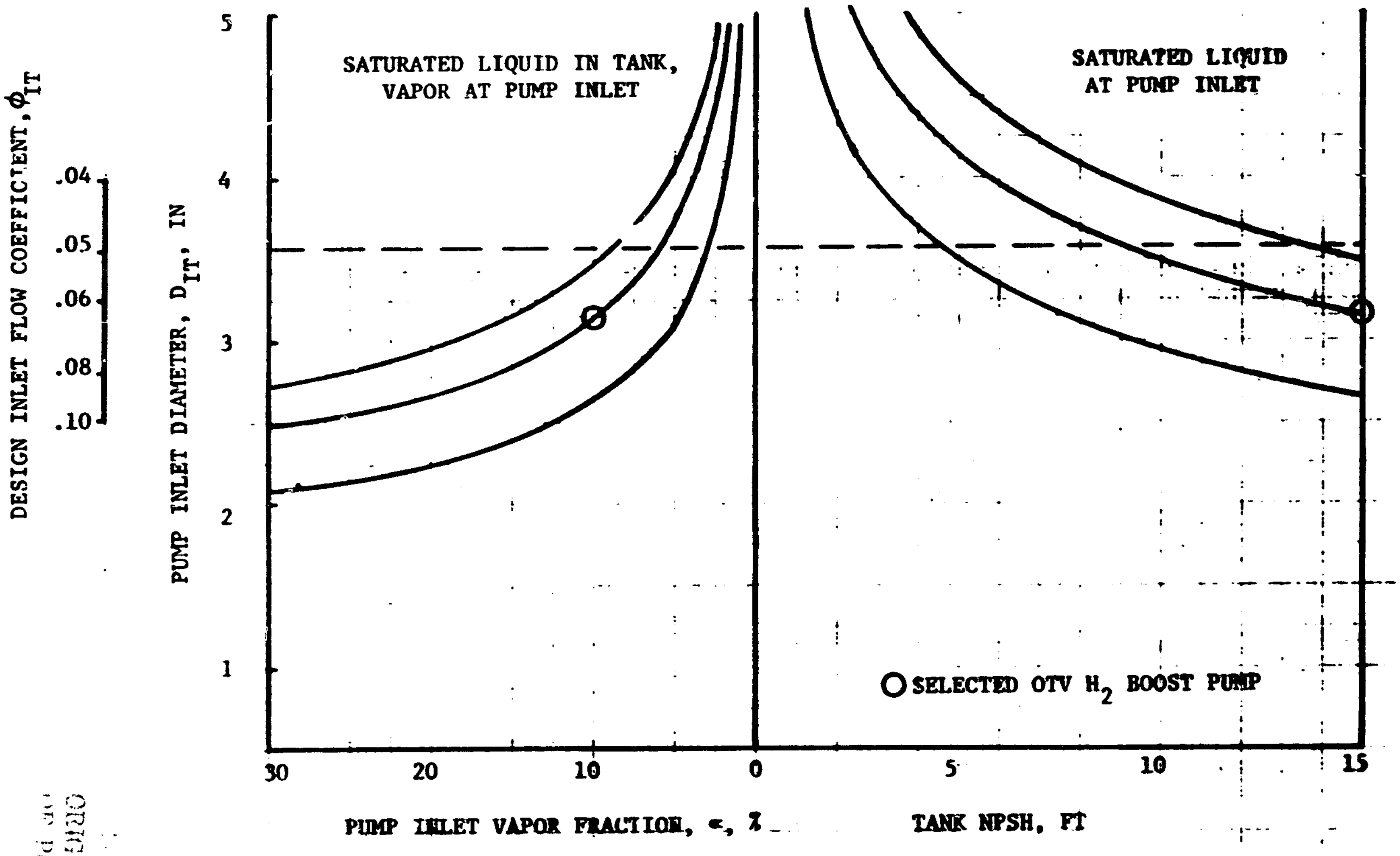


Figure 2. OTV HYDROGEN BOOST PUMP INLET DIAMETER REQUIREMENTS

9.9 percent vapor while the inducer blade capacity was 27.5 percent vapor at full thrust. The procedures used are also presented in Appendix A. Also shown in Fig. 2 is that the OTV pump could operate at a finite NPSH below design with a vapor fraction delivered at the inlet of from 0 - 10 percent.

Off-Design, Pump Idle Operation

The OTV engine provides autogeneous tank pressurization at full thrust conditions sufficient to provide the required boost pump tank NPSH values of 15 and 2 feet on the hydrogen and oxygen sides, respectively. Engine starts occur in tank head idle followed by pumped idle mode where tank pressurization is initiated. Therefore, the engine boost pumps must operate with zero NPSH during the initial phases of the pumped idle mode. Thus, the pump suction performance at pump idle mode impacts boost pump design selection.

The OTV pump idle mode vapor fraction is small (less than 1 percent by volume, Appendix A) but the boost pump design should have the capability to pump it. The pump vapor fraction capacity is predictable if the liquid incidence-to-blade angle ratio (i/β) is less than 0.7 and is zero if i/β exceeds 0.8. According to test data correlations, small amounts of vapor are pumpable up to a ratio of 0.8, but a more certain prediction results as i/β approaches 0.7. The angle ratio is shown parametrically in Fig. 3 for both types of boost pump designs so far considered. It is seen that the finite NPSH boost pump designs have generally lower i/β values due to their higher design blade angles (β). The zero NPSH pump designs have higher i/β values which place more uncertainty in the prediction of their two-phase flow pumping capability at off-design conditions and their ability to handle small vapor fractions off-design. The higher finite NPSH pump designs have less uncertainty in meeting off-design vapor conditions because of their higher incidence-to-blade angle ratio.

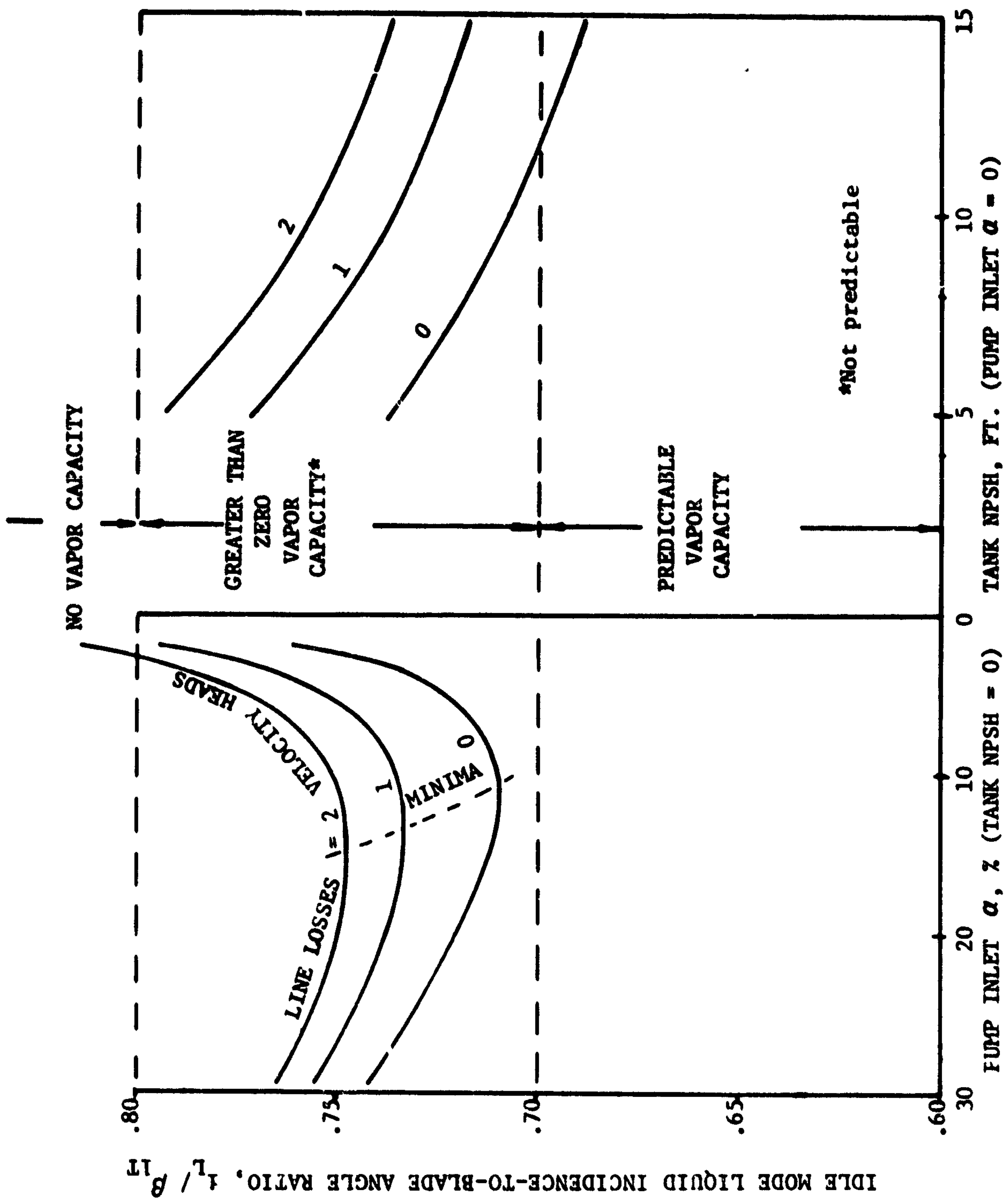


FIGURE 3. VAPOR CAPACITY PREDICTABILITY FOR OTV HYDROGEN BOOST PUMPS

Oxygen Boost Pumps with Zero Tank NPSH

Oxygen boost pump inlet diameters required for zero tank NPSH are indicated in Fig. 4. Also indicated in the figure are inlet diameters required for finite tank NPSH and vapor-free flow at the pump inlet. With zero tank NPSH, extremely high vapor fractions would be delivered to the pump inducer by the inlet pump diameter sizes that satisfy the minimum inlet flow coefficient requirement of .05. The selected OTV 15K thrust oxygen boost pump inlet diameter would deliver vapor fractions near 30 percent by volume, close to the limit of what the inducer could handle. See Appendix Table 7-31 for values in a specific vehicle duct configuration and vehicle acceleration.

The contract SOW did not require other NPSH designs to be examined for the oxygen boost pumps. The OTV oxygen boost pump designs at 2 NPSH and 15K lb thrust have two-phase pumping capabilities as indicated in Table 7-31 and 7-32 of Appendix A. No modifications to the oxygen pumps are recommended. The gains in tank pressurization system weights are small as indicated in Ref. 1.

SELECTED BOOST PUMP DESIGNS

For the following reasons, the 15-foot NPSH pump design is selected as the OTV 15K engine hydrogen boost pump design.

(1) The pump has a reasonably sized inlet diameter

(2) Its design inlet flow coefficient does not compromise on-design suction performance

(3) Its design blade angle provides low i/β angle values and thus less uncertainty in meeting suction performance off-design.

(4) It will operate satisfactorily at "zero" NPSH and 10% vapor and, therefore, could also be considered to be a "zero" NPSH design.

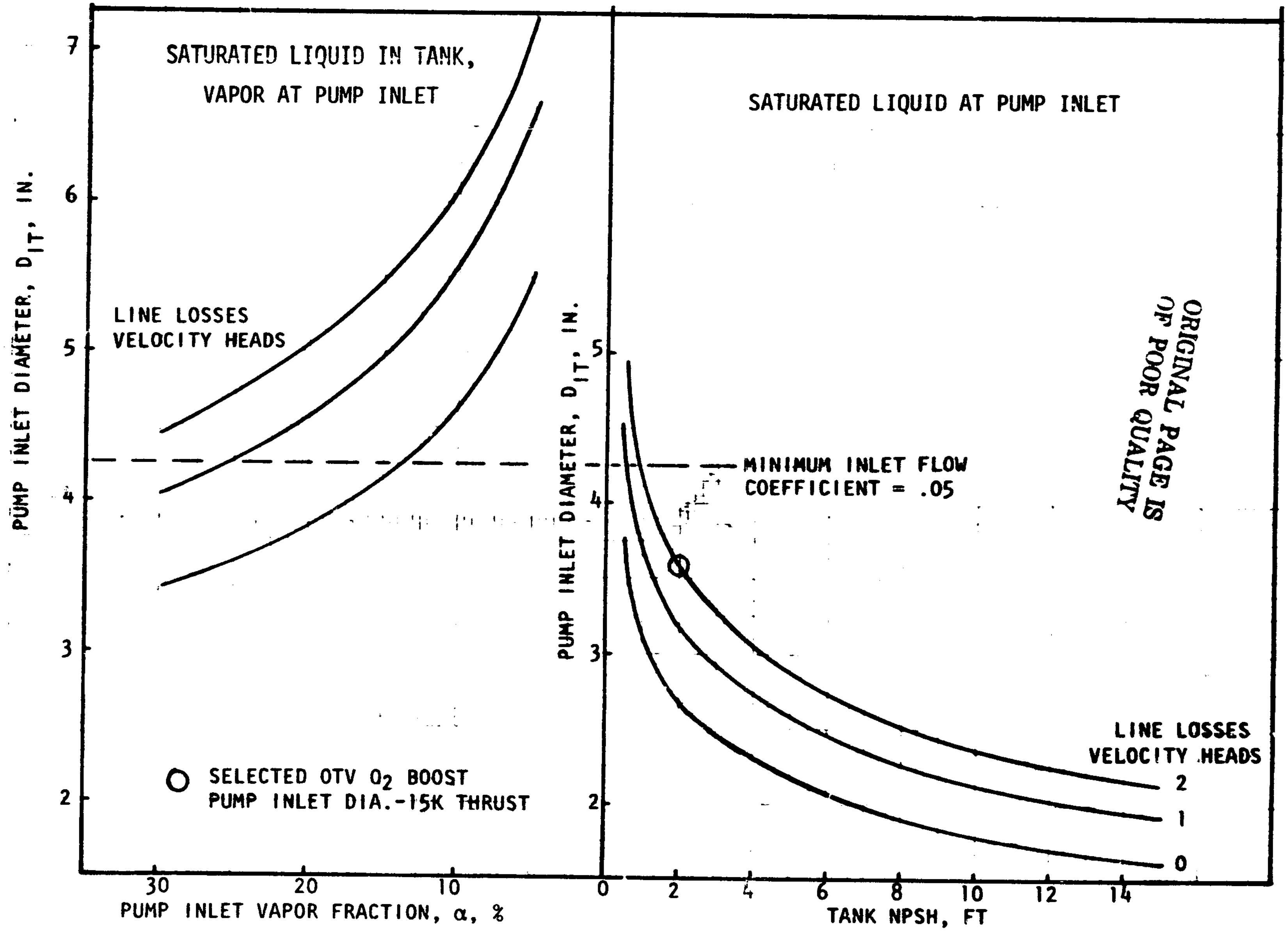


Figure 4. OTV OXYGEN BOOST PUMP INLET DIAMETER REQUIREMENTS

Since the recommended oxygen and hydrogen boost pump designs for operation at the NPSH conditions of Table 1 are the same as those previously selected in the OTV Engine Phase A studies, no impact on engine design is anticipated.

IMPACT ON PROGRAMMATICS

The cost and schedule impacts of low NPSH operation boost pumps on the overall OTV engine program were determined as increments with respect to the cost and schedule of the 20K lb thrust staged combustion engine previously submitted in Report RI/RD79-191-2, Volume II-B, and of the 10K lb thrust expander engine provided in report RI/RD80-155-2, Volume II-B. The programmatic impacts discussed below are identical for both engine cycles, staged combustion and expander, and for both thrust levels, 10 and 20K lb, since the impacts are small and only involve the engine low pressure pumps.

No cost or schedule impact was found for the low pressure oxidizer turbopump since the inlet condition for this pump (2 feet NPSH) is unchanged from the reference pumps.

Similarly, no cost of schedule impact was found for the low pressure fuel turbopump at pump inlet NPSH's of 5 and 10 feet liquid hydrogen. These inlet heads are considered to be not too different from the 15-foot NPSH reference point to influence pump design though they would lead to different pump dimensions and rotational speeds. These parameters do not change sufficiently enough to influence cost or schedule.

For the cases of zero NPSH, with pump inlet vapor fractions of 5, 10, and 20 percent, a small influence on DDT&E cost was determined, but no impact on schedule or on production or operation costs. Rocketdyne has successfully run turbopumps with as much as 30 percent vapor fraction in the inlet and no basic difficulties are expected in designing a fuel boost pump with 5, 10 or 20% vapor at the pump inlet. However, it is anticipated that about 10 percent more

design and analysis work needs to be performed and that about 20 percent more testing at the pump component level will be required to verify two-phase flow operation. The total DDT&E rough order of magnitude (ROM) incremental increase is estimated to be 0.60M $ (FY 1979 dollars), and is all contained in WBX element 1.1.1.3 (page 14 of RI/RD79-191-2).

TASK 15 - INTERMEDIATE THRUST OPERATION

The impact on engine design of a low thrust mode of operation requirement on the nominal thrust OTV engine was defined in this task. Both the expander cycle engine and the staged combustion engine were addressed. Detailed effort was carried out for the expander cycle engine with nominal thrust of 15,000 lb, and for the staged combustion engine at a nominal thrust of 20,000 lb. Conclusions for the other nominal thrusts of 10K, 15K and 20K were made qualitatively from these two detailed baseline cases. Each baseline case was examined at low thrust levels of 2000, 3000, 5000, and 7000 lb. Relative merits of kitting or throttling to achieve the low thrusts were addressed. From the design impact results obtained, the effect on DDT&E production and operation costs and schedules was determined. These costs and schedule changes were related to previous OTV Engine Phase A studies (Ref. 2) in the form of incremental values relative to the basic programs previously described in the reference.

IMPACT ON ENGINE DESIGN - EXPANDER CYCLE

Steady state and transient computer models were employed to assess the impact of intermediate thrust levels upon engine operation for an expander engine having a design thrust of 15,000 lb. Thrusts of 2000, 3000, 5000 and 7000 lb were considered. Qualitative analyses performed for design thrusts of 10K and 20K indicated the thrust levels where engine kitting becomes necessary and throttling alone cannot be used to achieve the desired thrust and mixture ratio levels.

An engine mixture ratio goal of 6.0 was selected for all intermediate thrust levels. This value was chosen for two reasons: (1) the design mixture ratio for low thrust OTV missions is 6:1,at the present time,for best utilization of available stage length, and (2) a mixture ratio of 6.0 represents a desired value in pump idle operation to maintain high engine specific impulse performance and overall delivered impulse for the high thrust OTV missions.

The results obtained using the transient simulation engine model with the OTV expander cycle engine of nominal thrust of 15,000 lbf indicated that: (1) at 2000 lbf thrust, stable engine operating conditions could be maintained only up to mixture ratio of 4.35:1; an excursion beyond this mixture ratio resulted in fuel-pump-generated instability; (2) different kittings of the engine showed satisfactory operating conditions at 2000 lbf thrust and a mixture ratio range from 5:1 to 6:1; and (3) at 3000 lbf thrust, stable engine operating conditions were achievable at mixture ratio of 6:1 without engine kitting.

The transient studies were concentrated at thrust levels of 2000 and 3000 lbf where unstable engine operating conditions are most likely to occur. The engine kits investigated at 2000 lbf thrust included removing the turbine inlet orifice, recirculating hydrogen in the main fuel pump and combinations of these options.

The expander cycle rocket engine dynamic computer code developed in NAS 8-33568 effort was utilized as a basic analytical tool for this part of the investigation. Some modifications were made to the model in order to reflect changes made to engine system by the engine kits. The steady-state engine operating conditions at 1800 lbf thrust and 4:1 mixture ratio (pumped-idle) were used as a nonzero time starting point for the transient simulations. The results of the dynamic analysis conducted are discussed in detail below.

Engine Operation at 2000 lbf

Dynamic analysis was performed at 2000 lbf thrust for the OTV expander cycle engine which has the baseline cycle configuration as shown in Fig. 5 . Open loop control of main oxidizer valve (MOV) and/or oxidizer turbine bypass valve (OTBV) were utilized for mixture ratio control, and thrust control was achieved by using the turbine bypass valve (TBV). A summary of cases run is presented in Table 2 . The first case represents the highest mixture ratio attained with stable pump operation.

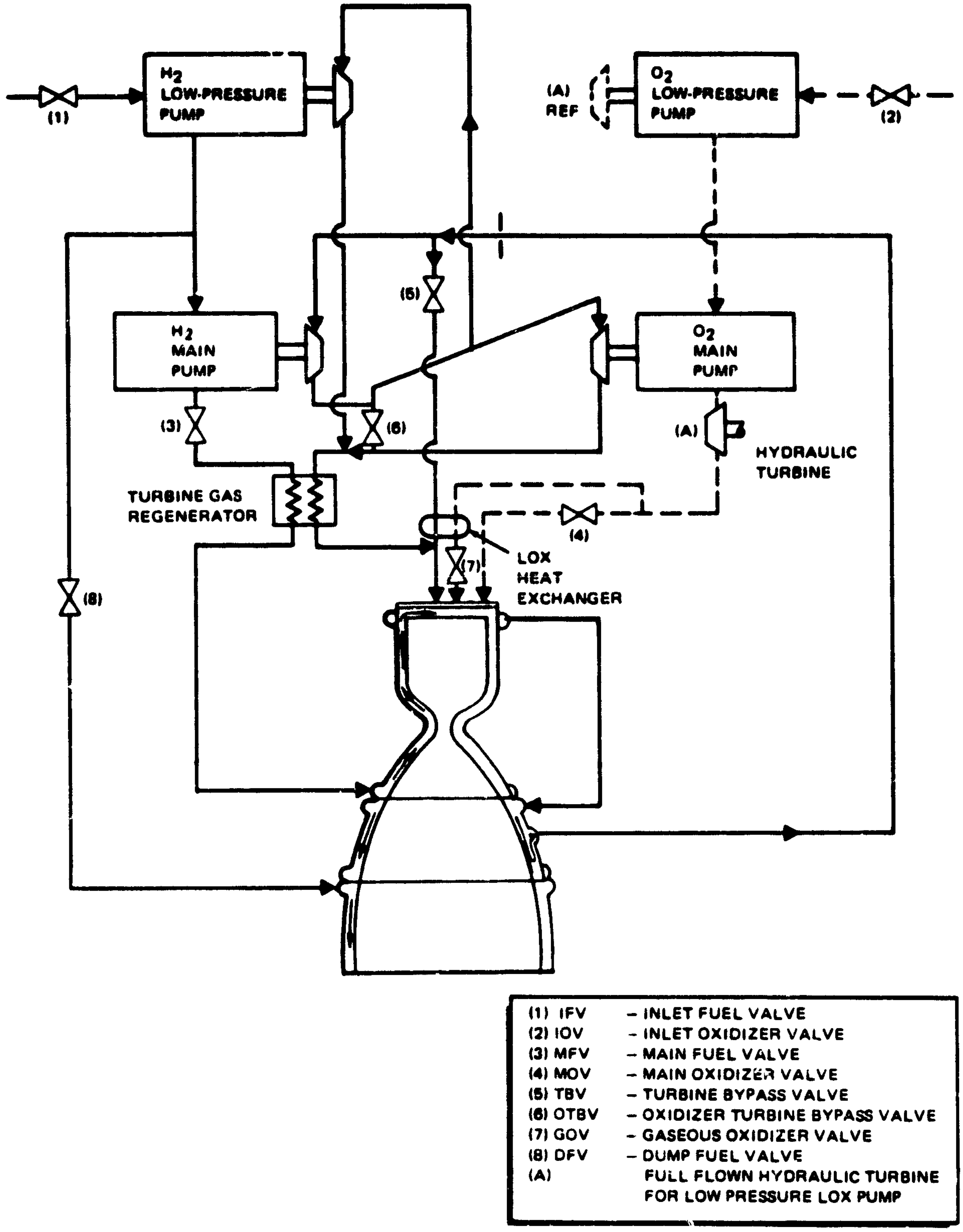


Figure 5. Expander Engine Schematic

TABLE 2. RESULTS OF TRANSIENT ENGINE MODEL RUNS AT 2000-LB AND 3000-LB THRUST, EXPANDER CYCLE

CASE NO.	ENGINE THRUST (lb_f)	ENGINE MIXTURE RATIO	DESCRIPTION OF ENGINE KIT REQUIRED	ENGINE OPERATING CONDITION	VALVE CONTROL RANGE
1	2000	4.35	None	Stable	Adequate
2	2046	4.68	None, MOV used for MR control	Unstable	Adequate
3	1998	4.99	None, OTBV used for MR control	Unstable	Adequate
4	2013	6.22	20% fuel pump recirculation	Stable	Adequate
5	2011	5.40	Remove turbine inlet orifice	Stable	Not adequate
6	2032	5.12	Remove turbine inlet orifice and 10% fuel pump recirculation	Stable	Not adequate
7	2038	6.3	Same as Case 6 with Turbine Orifice*	Stable	Not adequate
8	2985	6.06	None	Stable	Adequate

* See Figure 11

The second and third study cases were made using the MOV (Case No. 2) and OTBV (Case No. 3) independently for mixture ratio control with the unmodified baseline expander cycle engine configuration. Results show unstable engine operating conditions at engine mixture ratios of 4.7 and 5.0, respectively, due to fuel-pump-generated instability. The operating points for these cases are plotted in the main fuel pump H-Q map as shown in Fig. 6. As indicated, both Case 1 and Case 2 points are at some distance to the left of the peak of the H-Q curves. This part of the H-Q curves, having positive slope, represents a potential pump discharge flow instability and therefore may lead to unstable engine operation. Figure 7 and 8, which show the chamber mixture ratio profiles for the two cases being studied, indicate oscillatory instability in mixture ratio. Although the magnitudes of the oscillations shown are within 0.2 mixture ratio units, they are expected to grow at higher engine mixture ratio conditions. Unstable engine operation, therefore, is predicted for the baseline expander cycle configuration at 2000 lbf thrust and mixture ratios above 4.35. Engine kitting is therefore necessary for engine stabilization at the higher mixture ratios.

It has been found from cryogenic H_2 pump and engine tests and from engine transient model simulation studies of these tests that in order to achieve flow stability at the pump discharge, the pump-engine operating point needs to be close to the peak of the H-Q curves on the negative slope side. Unstable operating points to the left of the H-Q curve peak can be stabilized by either increasing the pump flowrate or reducing the operating speed, or both. The first approach requires some recirculation around the pump itself since the propellant flow requirements are determined by the selected thrust and mixture ratio which are fixed at 2000 lb and 6.0. The second approach suggests removing the turbine inlet orifice which reduces pump head requirements and allows more bypass around the turbine to maintain a constant thrust level, both of which reduce the pump operating speed. The third approach is a combination of the first two. Possible engine kittings drawn from these three approaches were studied and are discussed below.

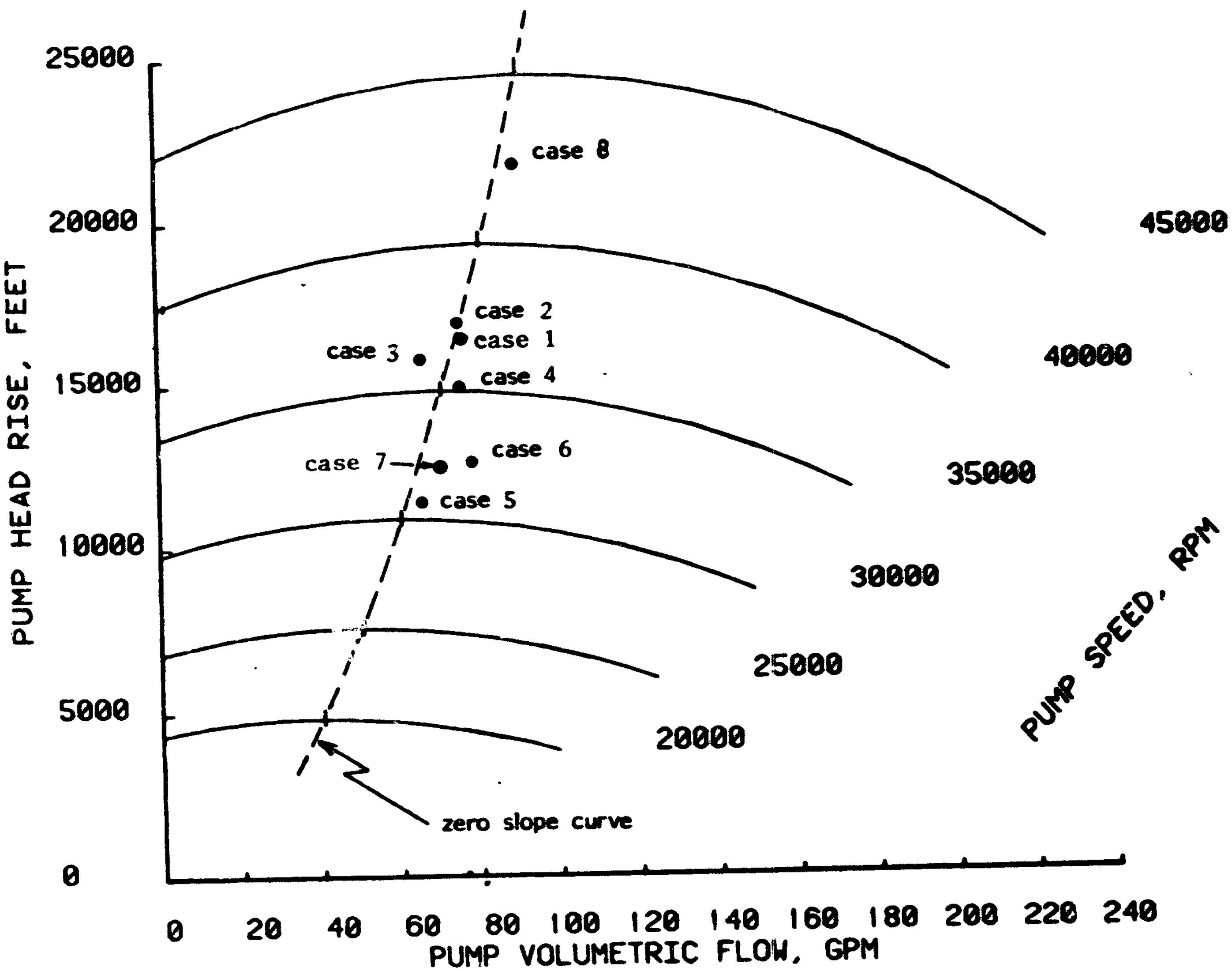


Figure 6. Hydrogen Pump H-Q Operating Maps and Transient Engine Model Runs

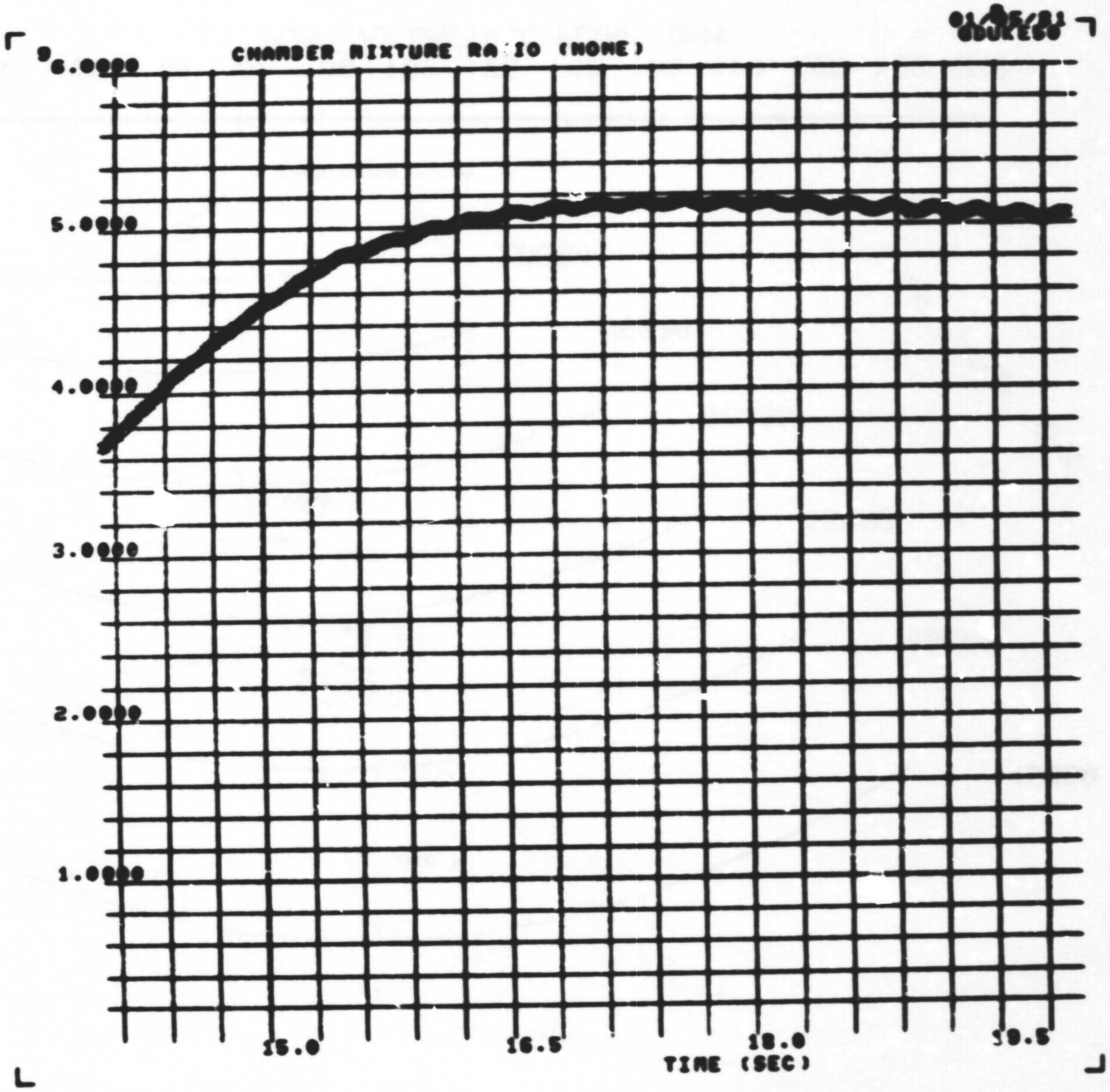


Figure 7. Unstable Thrust Chamber Mixture Ratio Trace, MR = 4.68, Thrust = 2046 lbs.

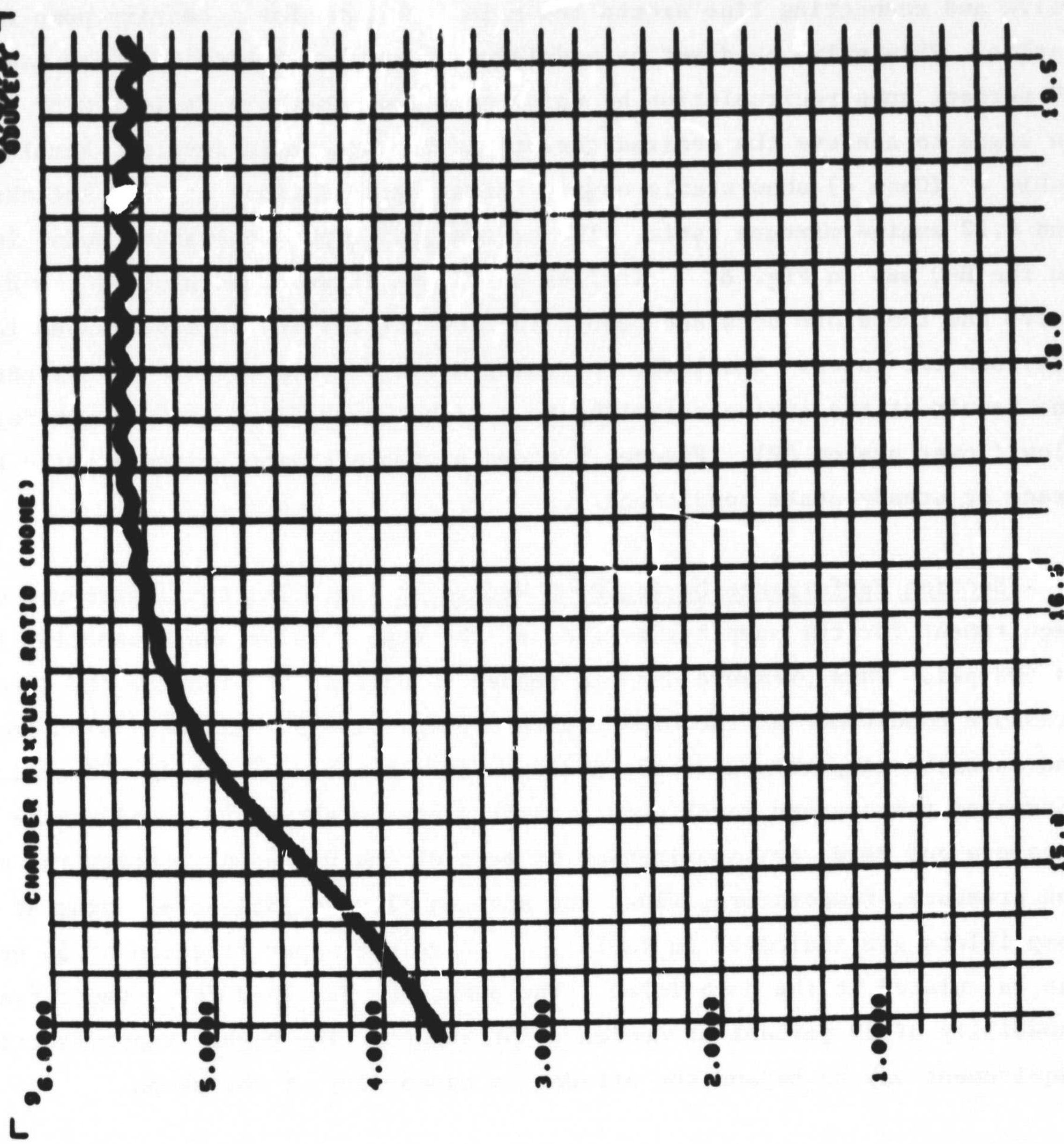


Figure 8. Unstable Thrust Chamber Mixture Ratio Trace, MR = 4.99, Thrust = 1998 lb

Pump Recirculation Kit at 2000-lb Thrust. The first engine kit consists of a valve and connecting line around the main fuel pump for necessary pump recirculation. This valve need not be modulated. Dynamic simulations were made with 20 percent pump recirculation by using open loop controls of TBV, OTBV and MOV in steps to achieve the desired thrust and mixture ratio levels. Results in Table 2 (Case 4) show stable engine operating conditions at 2013 lbf thrust and 6.22 engine mixture ratio. The corresponding pump operating point is shown in the H-Q map in Fig. 6. It lies on the positive slope side of the H-Q curve and therefore does not result in flow instability as experienced in the previous two cases. The lower operating hydrogen pump speed for this case is the result of the engine adjusting to a higher pump flow, and a lower injector flow (lower system ΔP). Figure 9 shows a stable chamber mixture ratio time-trace at steady-state conditions.

Suction Performance During Pump Recirculation. The total pressure drop requirement for the pump bypass flow at the bypass valve and connecting line is 508 psi. This pressure drop is needed to diffuse the flow to the inlet pressure conditions at the high pressure pump. The associated throttling process increasesthe temperature of the hydrogen stream after mixing is effected. The increased temperature results in a vapor pressure above the mixed stream static pressure and leads to vaporization of part of the hydrogen entering the pump. The pressure, temperature, flow, and suction flow conditions existing at the pump inlets are indicated in Table 3. A volume vapor fraction of 50 percent was calculated at the pump inlet. The pumps are designed for a vapor fraction capability of 20 percent at design point thrust. The higher vapor fraction requirement may be beyond the off-design capability of the pumps.

To reduce the vapor fraction in the flow some pressure recovery is required in the pump recirculation line to reduce the power losses. This can be accomplished through use of a jet-pump for momentum exchange between primary and recirculating flows. The recirculation may also be accomplished between the

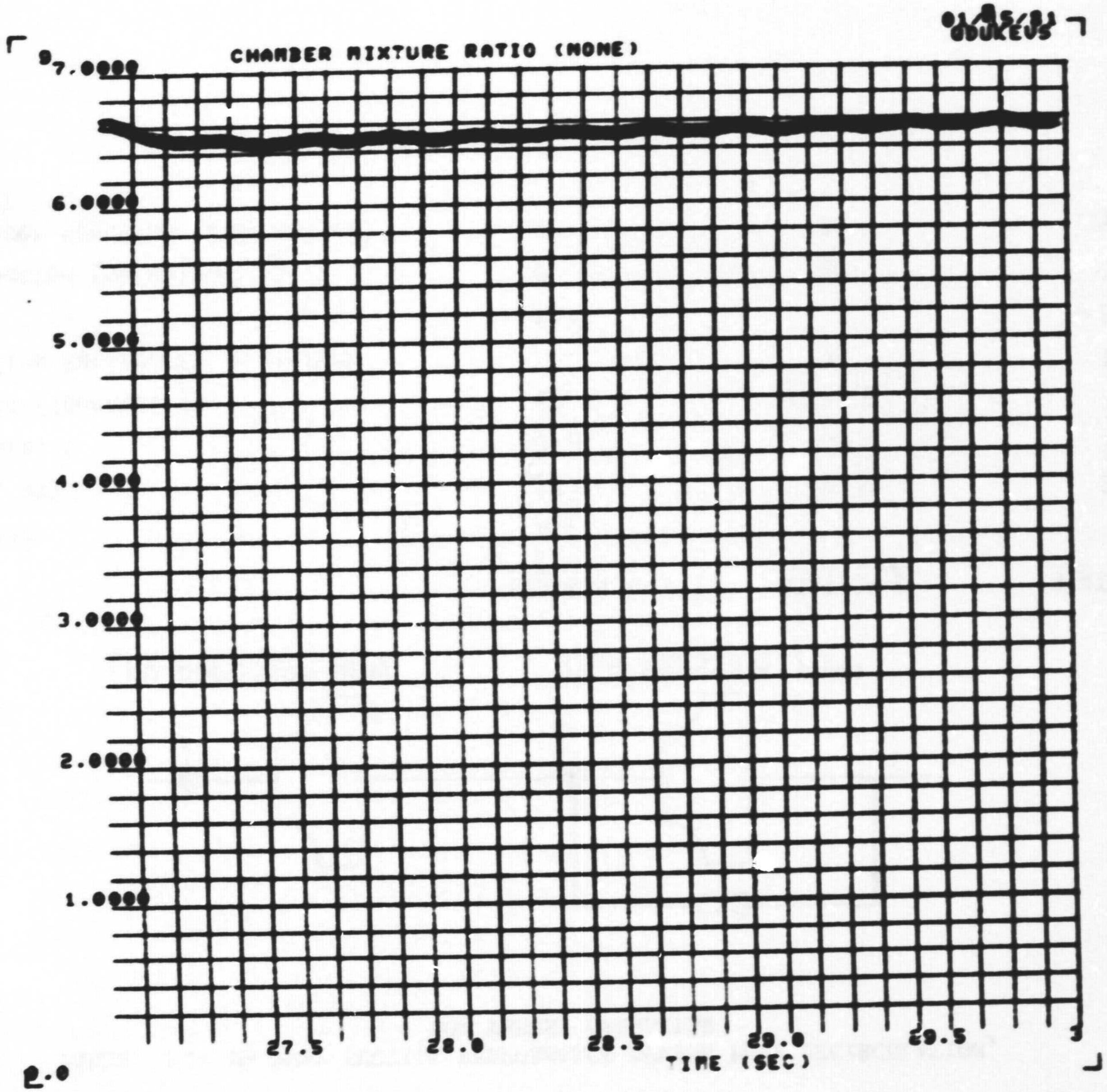
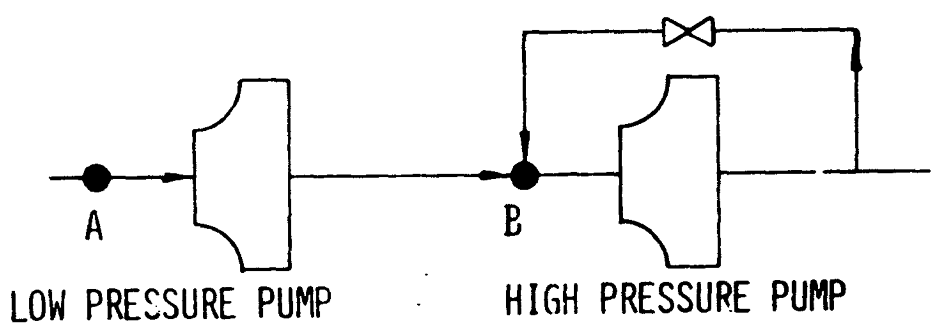


Figure 9. Stable Thrust Chamber Mixture Ratio Trace with Pump Recirculation, MR = 6.22, Thrust = 2013 lbs.

TABLE 3. H_2 PUMP SUCTION PERFORMANCE DURING PUMP RECIRCULATION, LOW THRUST OPERATION



	STATION A	STATION B	STATION B
Case Number	All	3	7
Pressure, psia	18.8	22.7	23.2
Temperature, R	37.8	38.4	39.2
Volumetric Flowrate, gpm	63.0	75.6	70.2
Percent Flow Recirculation in Pump	--	20	10
NPSH, ft	15	--	26.7
Vapor Fraction (by volume), %	0	50	0
Design Vapor Fraction (% by volume) Capability	20	20	20

pump exit stage and an intermediate pump stage like the inducer stage exit to reduce the pressure losses and thus lower the amount of vapor in the flow.

Engine System Pressure Drop Reduction. To increase the stability of the engine pumps, the engine operating line (head vs flow) must be to the right of the zero-slope pump curve in Fig. 6. One way of achieving this is to reduce engine system pressure drop at constant thrust and in that manner reduce pump head and speed. The engine kitting involved in this case consists of removing the turbine inlet orifice used to obtain a system pressure drop margin at full thrust. This action lowers the pump power demands and for the same system flowrates allows the turbines to operate at lower pressure ratios. The latter results in lower pump discharge pressures and speeds as needed for stable operation. Thus, stable engine operating conditions can be maintained at 2011 lbf thrust and 5.40 engine mixture ratio as in Case 5 in Fig. 6 and Table 2. The mixture ratio-time trace (Figure 10) for this case is stable.

The reduced pump discharge pressure reduces the TBV pressure drop and flowrate. To maintain thrust at a level of 2000 lb as in Case 3, the turbine bypass valve (TBV) must be opened to the limit of its control range (90% open). To increase mixture ratio above that of Case 3, the MOV and OTBV also must be exercised to the limit of their control ranges; 100% open and 0% open, respectively. Thus, for Case 5 a mixture ratio of 5.4 represents the limit in mixture ratio attainable since no mixture ratio control remains.

Pump Recirculation and System Pressure Drop Reduction. Pump recirculation results in large flow vapor fractions while removal of the system pressure drop margin orifice reduces the thrust and mixture ratio controlability. Less recirculation than in Case 4 is desirable if combined with system pressure drop reduction as in Case 5.

Engine dynamic studies indicate that stable engine operation can be maintained at 2032 lbf thrust and 5.12 mixture ratio, with 10% pump recirculation. The

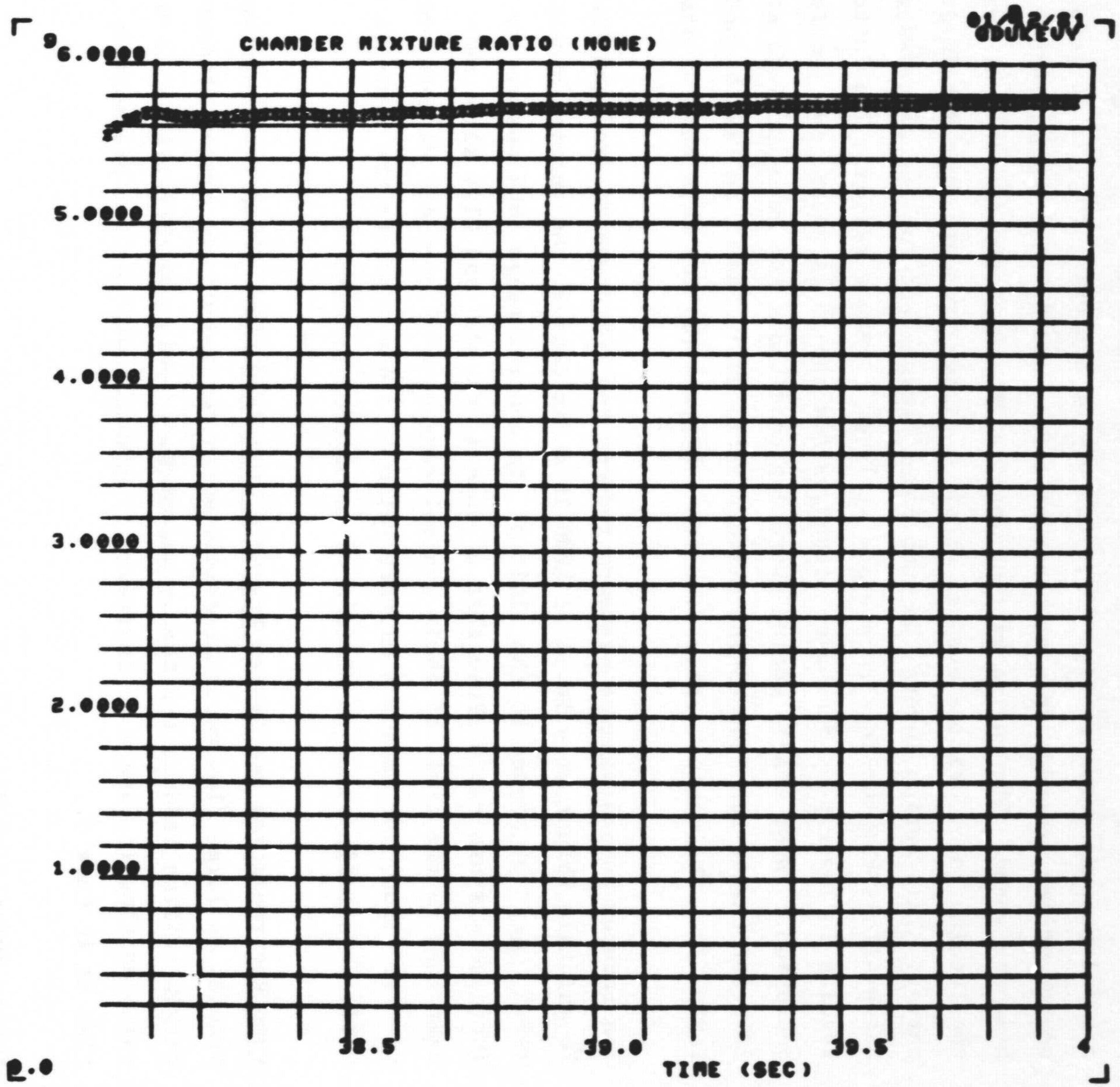


Figure 10. Stable Thrust Chamber Mixture Ratio Trace with Reduced System Pressure Drop, MR = 5.4, Thrust = 2011 lbs

engine operating point is plotted in the H-Q pump map (Figure 6) for this case (Case 6). However, as in Case 5, no mixture ratio control margin is left and engine mixture ratios higher than 5.12 are not possible with the engine configuration as defined so far.

A new orifice and line inserted between the hydrogen feed line to the turbines and the main oxidizer turbine (Figure 11) provides increased flow to the oxidizer turbine which, if bypassed at the oxidizer turbine bypass valve, allows that valve to open and regain control. The orifice also bypasses flow around the fuel turbine and returns thrust control to the turbine bypass valve. Case 7 in Figure 6 is the fuel pump H-Q conditions for such a case. Pump stability and valve control are thus achieved with less pump recirculation and lower system pressure drops. The improved pump suction conditions for this case are indicated in Table 3.

15K-Engine Operation at 3000 lb

Dynamic studies were conducted at 3000 lbf thrust for the baseline expander cycle configuration (Figure 5). Open loop controls of TBV, MOV and OTBV were employed in order to achieve desired thrust and mixture ratio levels. Results show satisfactory engine operating conditions can be maintained at 2985 lbf thrust and 6.06 engine mixture ratio. The fuel pump operating point is also shown in its H-Q map (Case 8 in Figure 6) for this case.

Stable engine operation can also be expected at 5000 and 7000 lbf thrust and 6:1 mixture ratio. This is because the higher fuel flow requirements at these thrust levels will provide better pump stability than in the case of the 3000 lbf thrust level.

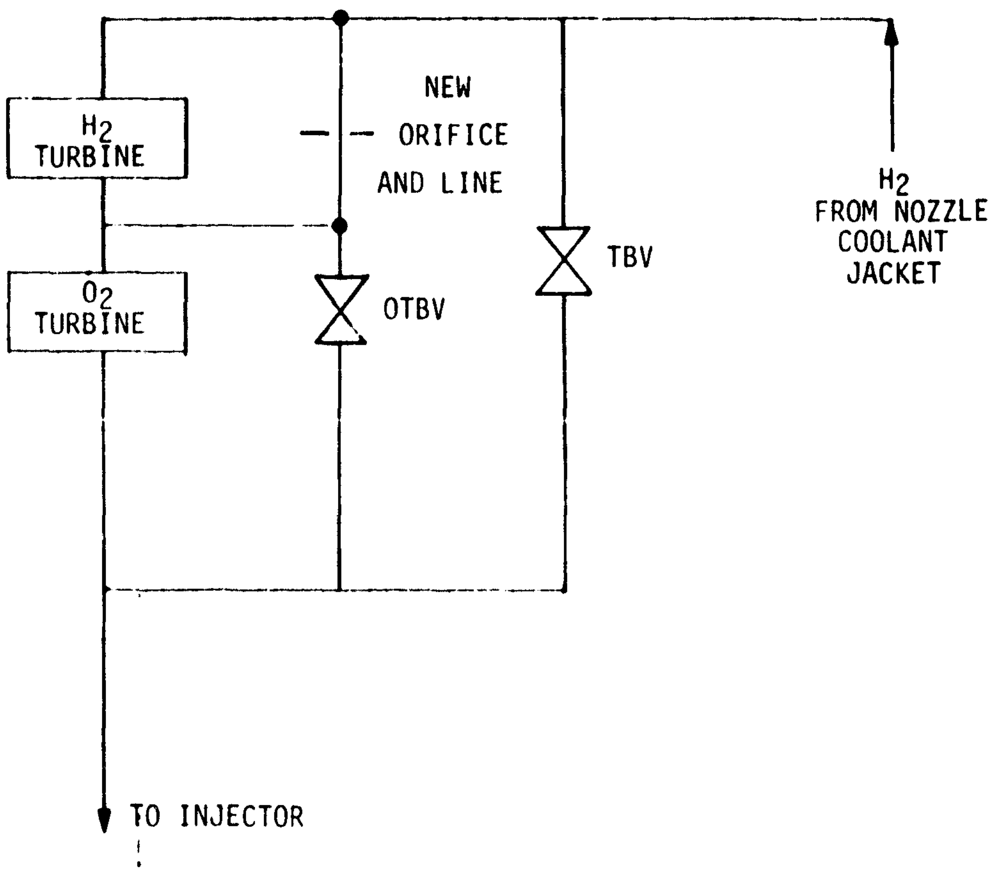


Figure 11. Fuel Turbine Bypass Flow Orifice

15K Engine Operation at Other Thrusts

The steady-state expander cycle off-design code was used to investigate engine balance points at engine mixture ratio of 6:1 and thrusts of 3000 lb and above for a design thrust of 15,000 lb engine. The steady-state code was used for the higher levels of intermediate thrust due to its smaller cost per case and because detailed examination of transient balances indicated no instability problems at thrust levels above 3000 lb. The 3000-lb thrust level was also examined with the steady-state model to provide a case for comparison with the transient balance results.

Important data resulting from steady-state balances at intermediate thrust levels included pump operating points, flowrates, engine performance, and control inputs required. Table 4 presents a summary of important operating parameters predicted at each intermediate thrust level, along with the design values for comparison.

Control Requirements. Control valve position and resistance requirements for achieving each intermediate thrust level are shown in Table 5 .

TABLE 4 . CONTROL VALVE POSITION REQUIREMENTS

THRUST	MFV POSITION %	$\bar{R}$	MOV POSITION %	$\bar{R}$	TBV POSITION %	$\bar{R}$	OTBV POSITION %	$\bar{R}$
2000*	100	1.0	80.9	.995	83.5	.006	77.0	1.50
3000	100	1.0	78.2	1.299	80.2	0.009	64.9	6.57
5000	100	1.0	80.7	0.983	74.9	0.017	65.3	6.30
7000	100	1.0	83.2	0.666	69.7	0.031	65.7	5.95

NOTE: $\bar{R} = \frac{\text{Valve Resistance}}{\text{Valve Design Resistance}}$

*Using 20% recirculation of hydrogen

TABLE 5 . 15K ENGINE OPERATING PARAMETERS, ON-AND-OFF DESIGN

PARAMETER \ THRUST LEVEL	DESIGN 15,000	2000*	3000*	5000	7000
Engine Specific Impulse	480.8	452.7	456.9	465.7	469.2
Engine Mixture Ratio	6.0	6.0	6.0	6.0	6.0
Chamber Pressure psia	1,540	215	320	520.5	728
Throat Area, in^2	4.697	4.697	4.697	4.697	4.697
Area Ratio	625	625	625	625	625
Thrust Chamber Thrust, lb	14,807	1984	2973	4923	6897
Dump Coolant Thrust, lb	193	16	27	77	103
Thrust Chamber MR	6.59	6.33	6.38	6.67	6.64
Thrust Chamber Isp	481.2	453.2	457.4	465.9	469.5
Inject. Fuel Flowrate, lb/sec	4.05	0.597	0.880	1.377	1.923
Injector Oxid. Flowrate, lb/sec	26.71	3.781	5.62	9.189	12.77
Fuel Pump Speed, RPM	110,000	35,329	42,233	54,435	65,189
Fuel Pump Efficiency	0.64	0.52	0.55	0.60	0.63
Fuel Pump Horsepower	1,585	39	65.7	155	291
Fuel Pump Pressure Rise, psi	4,597	508	682.3	1,138	1,623
Oxid. Pump Speed, RPM	52,837	14,401	18,155	24,000	29,936
Oxid. Pump Efficiency	0.666	0.52	0.57	0.628	0.65
Oxid. Pump Horsepower	384	6.1	12.9	32.15	65.1
Oxid. Pump Pressure Rise, psi	2,562	222.8	349	588	890
Fuel Turbine Inlet Temp., R	875	890	873	841	808
Fuel Turbine Flowrate, lb/sec	3.64	0.35	0.39	0.73	1.17
Fuel Turbine Pressure Ratio	1.72	1.31	1.30	1.36	1.41
Fuel Turbine Efficiency	0.636	0.379	0.532	0.599	0.642
Oxid. Turbine Inlet Temp, R	797	865	839	799	761
Oxid. Turbine Flowrate, lb/sec	2.99	0.267	0.34	0.64	1.02
Oxid. Turbine Pressure Ratio	1.21	1.06	1.07	1.09	1.11
Oxid. Turbine Efficiency	0.629	0.326	0.448	0.517	0.572

*F = 2000 modeled with transient code; F = 3000 modeled with both steady-state and transient codes.

Note that the 2000-lb case utilized hydrogen recirculation. This causes control requirements for this thrust to depart from the general trend shown by the other thrust levels, in which increasing thrust results in progressive opening of the OTBV and MOV. In all cases, increasing thrust requires closing of the main turbine bypass valve (TBV), which is the principal thrust control valve.

Pump Operation. Pump required head rise and volumetric flow is tabulated in Table 6 for each thrust level.

Table 6 . Pump Required Head and Flow

	FUEL PUMP				OXIDIZER PUMP			
THRUST	SPEED RPM	FLOW GPM	HEAD FEET	EFFI-CIENCY	SPEED RPM	FLOW RPM	HEAD FEET	EFFI-CIENCY
15,000	110,000	420.3	134,635	0.640	53,000	170	5170	0.67[illegible]
2,000*	35,329	75.6	16,625	0.537	14,400	23.7	451.9	0.508
3,000	42,233	92.8	21,931	0.551	18,155	36.2	707.5	0.569
5,000	55,875	155.1	37,857	0.609	24,000	58.7	1194	0.628
7,000	67,806	216.3	54,671	0.631	29,936	81.43	1805	0.650

*Using 20% Hydrogen Recirculation

These points are plotted on pump maps in Fig. 12 through 15 . The maps shown correspond exactly to the analytical pump characteristics contained within the engine balance models used.

The high pressure fuel pump operating points are shown in Fig. 12 and 13 . The data points for the 3000, 5000, and 7000 lb thrust levels and engine mixture ratio of 6:1 are all in the stable region, although the 3000-lb thrust point lies rather close to the zero slope point. A 2000-lb thrust point is shown for a case employing 20 percent recirculation and is also within the stable region. Also shown is a point representing 2000-lb thrust without hydrogen recirculation. This point is in the positive-slope region, indicating instability. Transient model runs of this case exhibit oscillatory instability as expected and previously discussed.

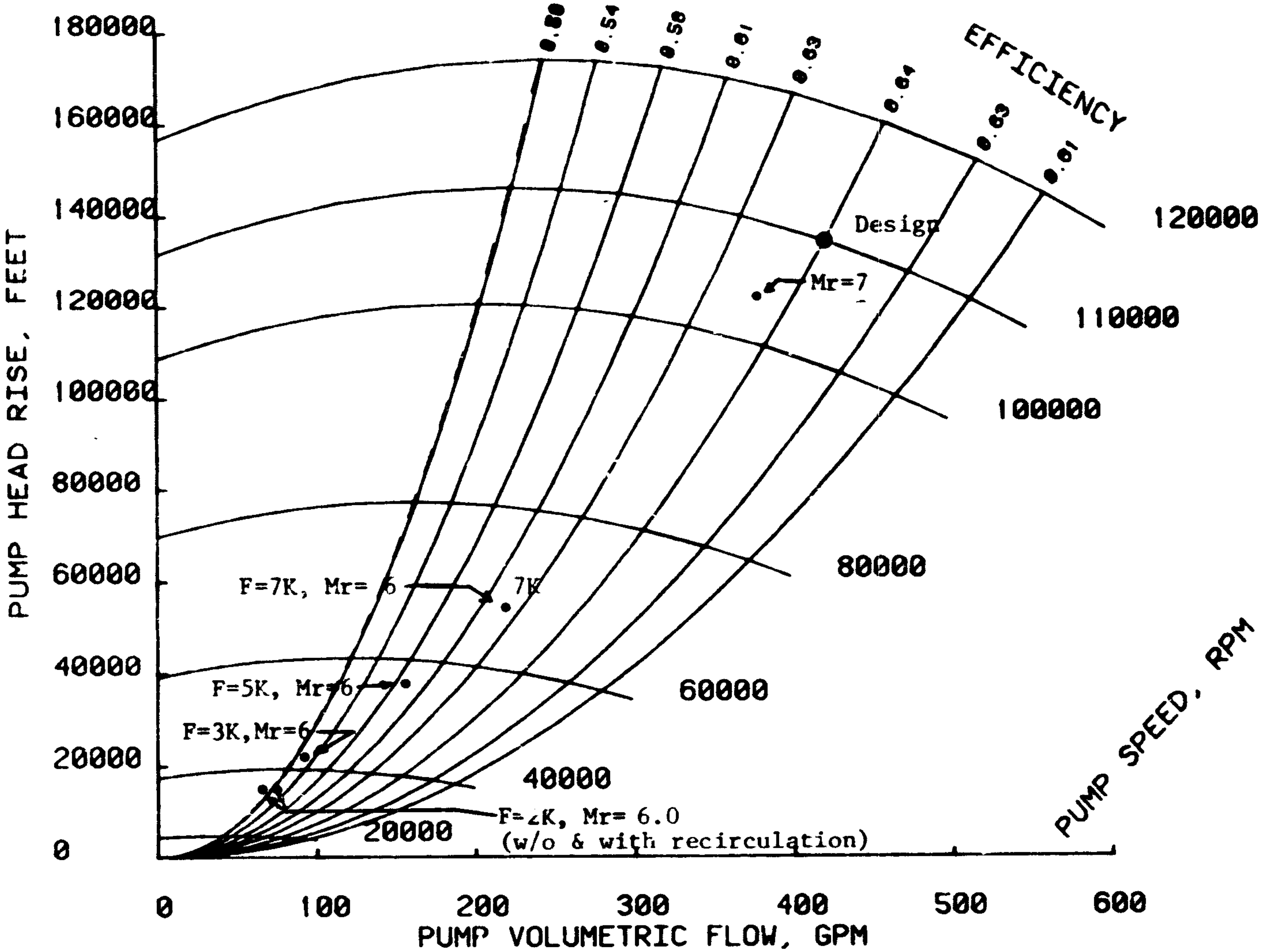


Figure 12 . Fuel Pump Characteristics for 15-K Design Thrust Engine

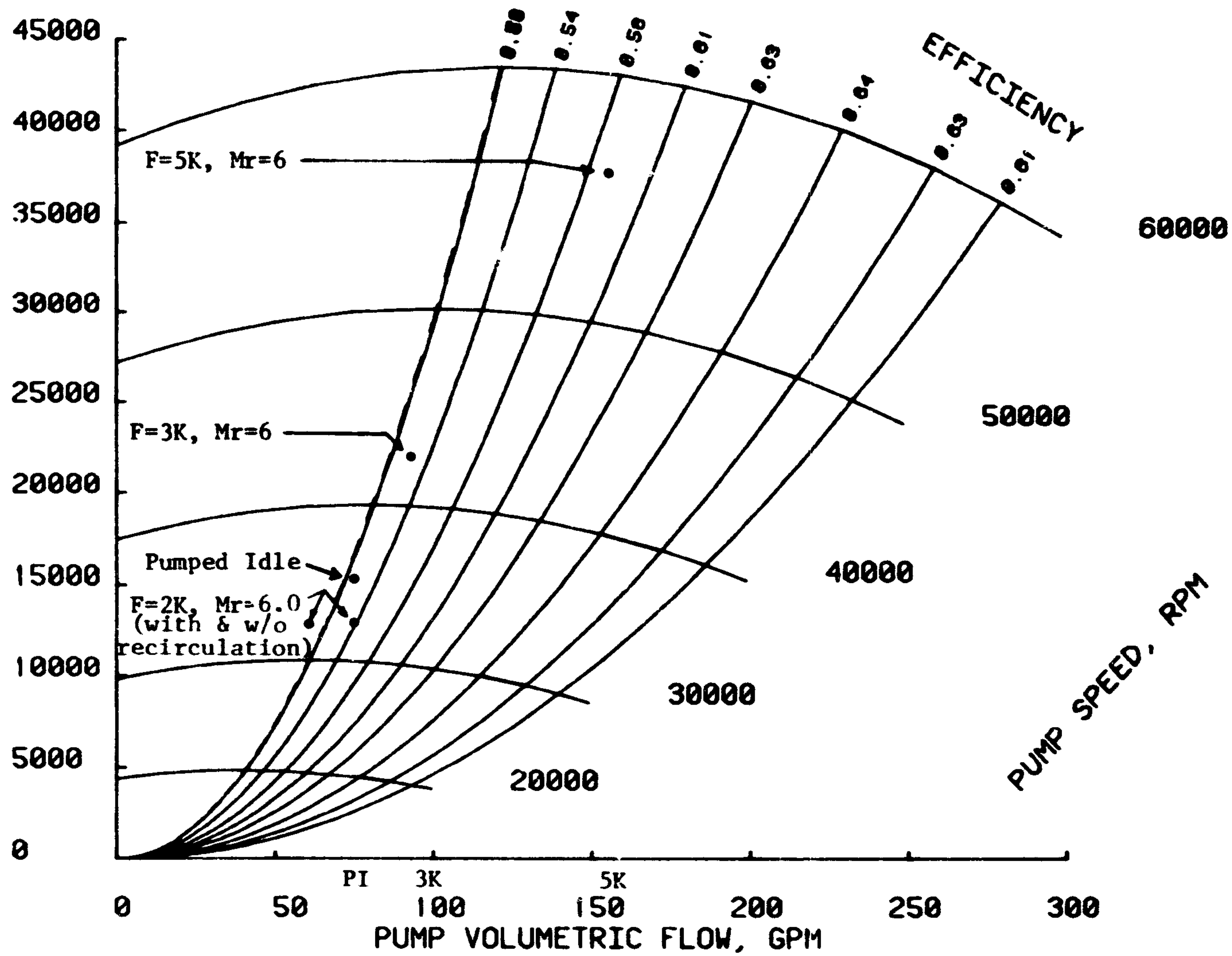


Figure 13. Fuel Pump Characteristics at Low Flows, 15K Design Thrust Engine

The high pressure oxidizer pump operating points are shown in Fig. 14 for all the thrust levels investigated at an engine mixture ratio of 6:1. The pump operation is stable in the range of interest 2000 - 15,000 lb. The pump is well within the stable H-Q region at the lowest thrust of 2000 lb as was also shown to be the case in the transient model runs.

Engine Design Thrust Levels of 10K and 20K

For each of the engine design thrust levels of 10K, 15K, and 20K, the fixed set of intermediate thrusts chosen (2K, 3K, 5K, 7K) represent a different fraction of design thrust (F/F_D). For a 10,000-lb design thrust level, for example, the 2000-lb intermediate thrust represents $F/F_D = 0.2$. For $F_D = 20,000$ lb, the 2K thrust is only at $F/F_D = 0.10$. Because fuel pump volumetric flow is nearly linear in thrust at fixed M_R, it can be seen that a design thrust of 20,000 lb represents the "worst case" with respect to stable low thrust operation, and 10,000 lb the best case.

In the 15,000-lb design case, F/F_D values above .20 were found to be stable without engine modification. Using this thrust fraction as a stability guide, it would be expected that all intermediate thrust levels will be stable without modifications for the design thrust level of 10,000 lb and that the 5000- and 7000-lb thrust cases will be stable for the 20,000-lb design. Thrusts below 4000 lb ($F/F_D = 0.2$) will require kitting to assure stability at the 20,000-lb thrust design level.

This reasoning is confirmed by referring to Fig. 15 and 16 which show the H-Q maps for the high pressure fuel pump at thrusts of 10K and 20K. The pump design parameters are summarized in Table 7. Figure 15 shows that all thrust levels from 2K to 7K are within the stable region for a mixture ratio of 6.0 and a design thrust of 10K without recirculation of hydrogen. The plot in Fig. 16 illustrate the situation for the 20K design thrust. Both the 2K and 3K points are seen to lie in the positive-slope (unstable) region of the pump map, and

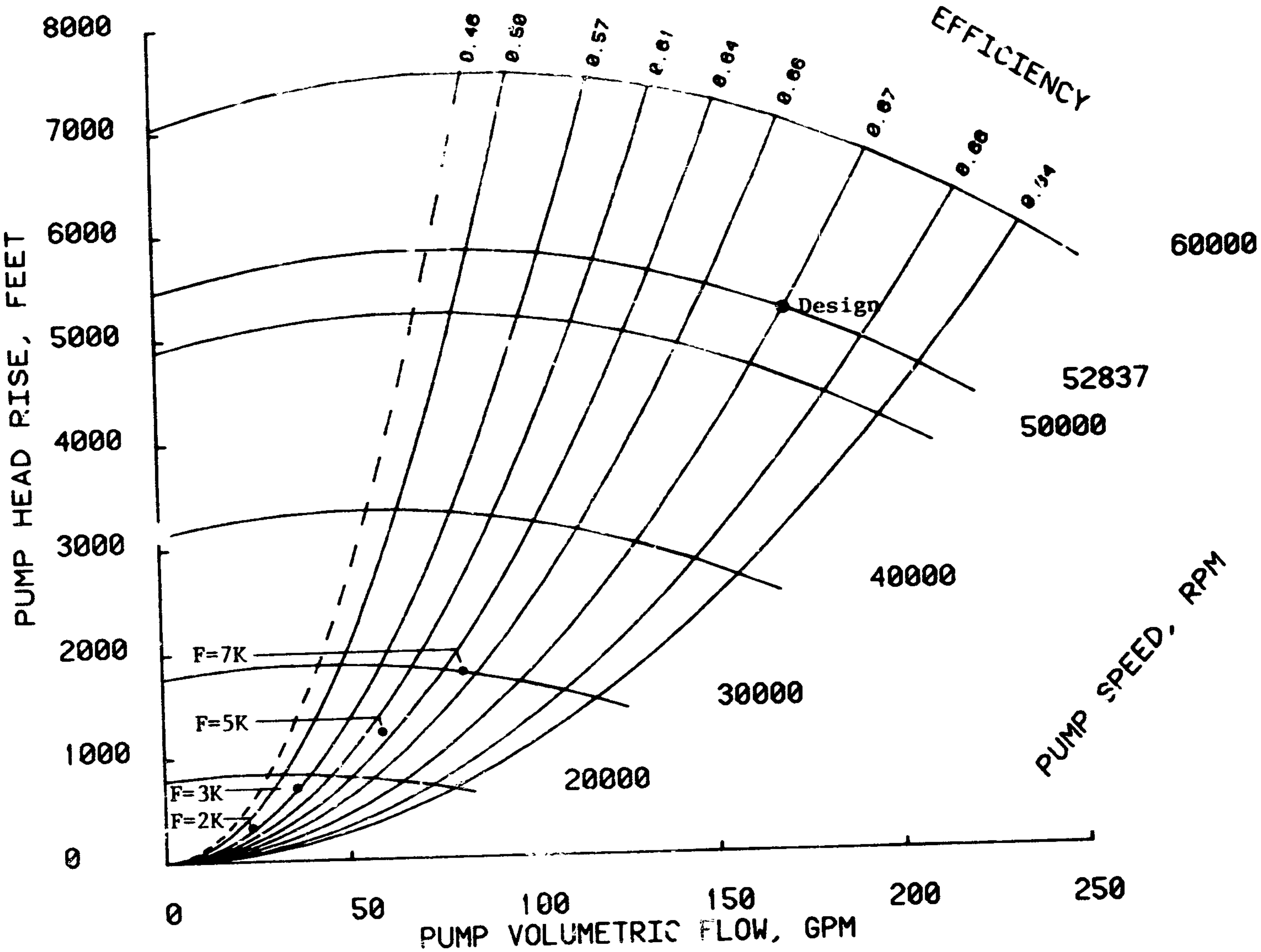


Figure 14. Oxidizer Pump Characteristics, 15K Design Thrust Engine

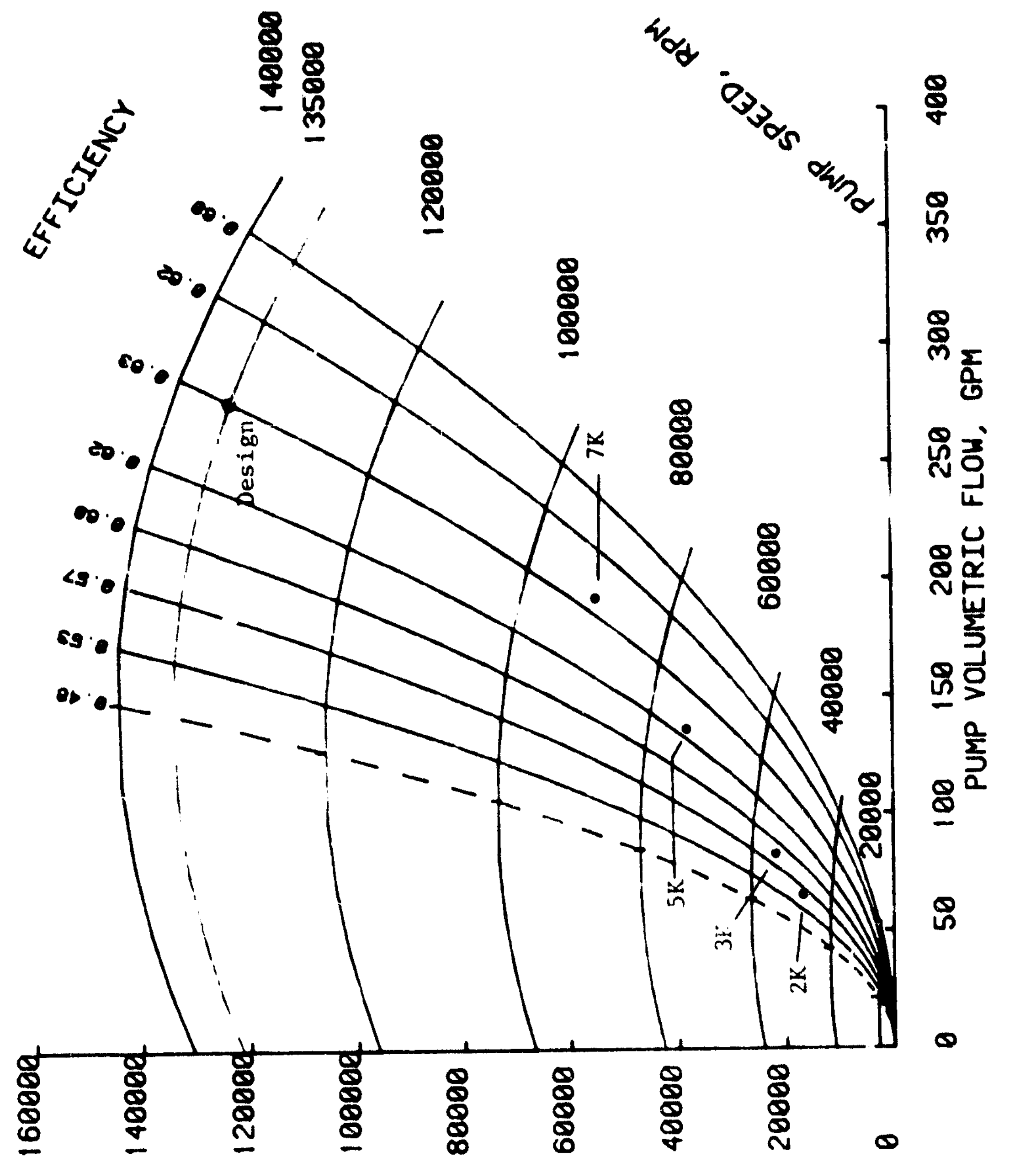


Figure 15. Fuel Pump Characteristics, 10K Design Thrust Engine

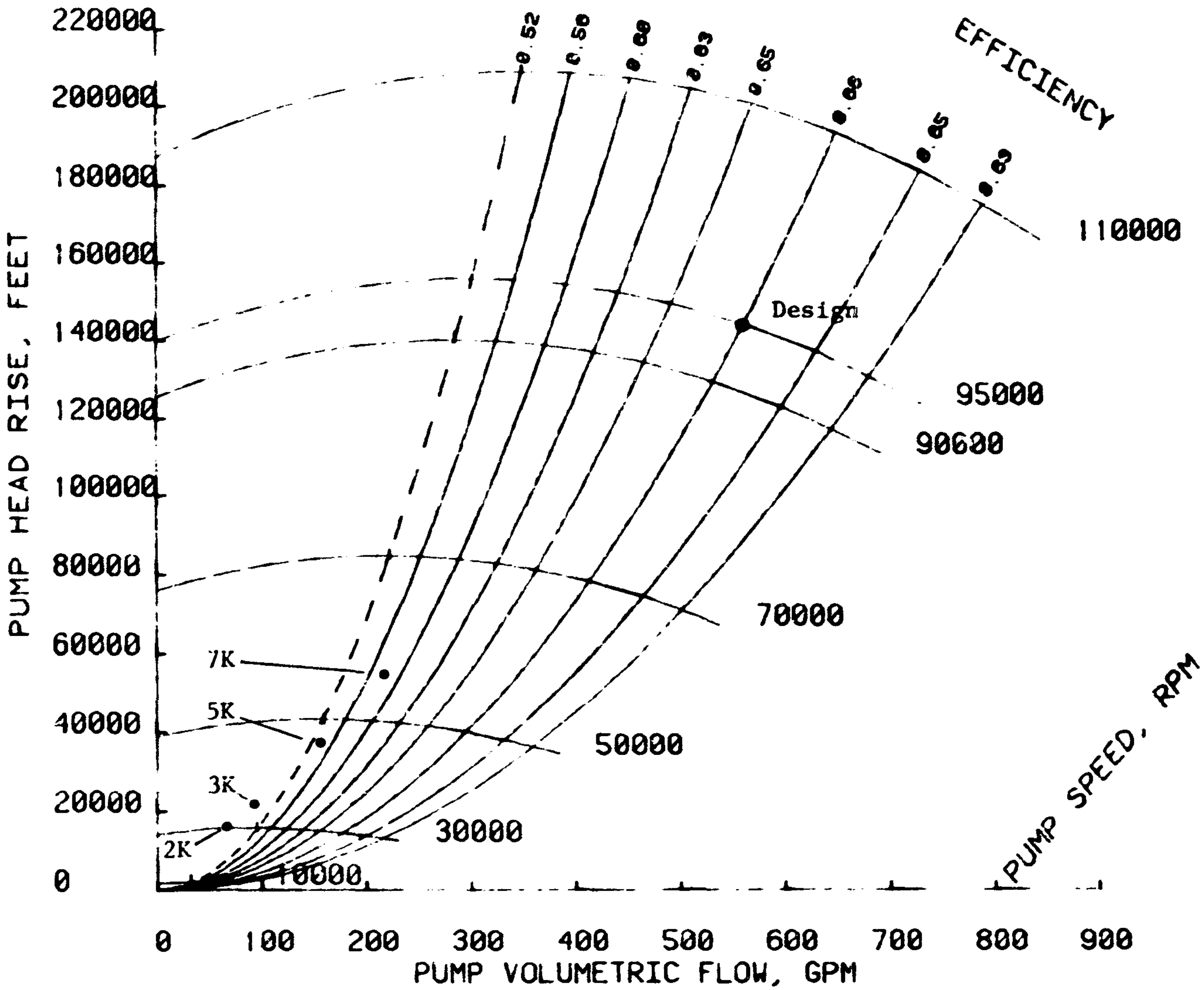


Figure 16. Fuel Pump Characteristics, 20K Design Thrust Engine

therefore will require recirculation to increase the flowrate so as to place the operating points in the stable region. It is estimated that 20 to 25 percent of the hydrogen flow would have to be recirculated for these thrust levels in order to achieve stability, with other modifications made to the system to reduce engine system pressure drop. The vapor fractions resulting from this amount of recirculation can be reduced somewhat by the use of a jet pump or recirculation between high-pressure stages.

Oxygen pump stability is not a problem at any of the design intermediate thrusts examined for the 10K and 20K design thrust level engines.

TABLE 7. EXPANDER FUEL PUMP DESIGNS FOR 10K TO 20K THRUST

THRUST	P_c, PSIA	PUMP SPEED RPM	HEAD RISE FEET	FLOW RATE GPM	EFFICIENCY
10,000	1415	135,000	123,140	276.5	0.63
15,000	1540	110,000	134,675	420.3	0.64
20,000	1650	95,000	143,903	561.1	0.66

Injector Performance at Low Thrust

Analysis of the OTV-type injectors with the coaxial injector combustion model (CICM) computer code has shown that at 10 percent of design flows a loss in injector vaporization efficiency of approximately 1.64 points is to be expected. This loss is attributed to the low pressure drop across the oxidizer element and consequent low velocity of oxidizer injection. Reduction of the inside diameter of the rigimesh plate retainer nut, which also forms the injector element fuel cup, increases the fuel velocity and provides added momentum exchange to the oxygen at the lower flows. The increased velocity difference between the two

streams increases the injector performance at the lower thrusts. All the intermediate thrust engines will require this simple modification to optimize injector performance.

Low oxidizer injector pressure drop could also lead to engine system coupled instability of the "chug" type. With a relatively high pressure drop in the oxidizer valve mounted in close proximity to the injector manifolds, the chugging problem should be eliminated.

Combustor and Nozzle Cooling Requirements at Low Thrust

Thermal analysis of combustor, fixed nozzle, and extendable nozzle of OTV engine candidates has indicated that enough coolant is available at the low thrusts to effect MR = 6 cooling of all three components. With mainstage component coolant allocations, however, the combustor receives more than ample cooling at the lower thrusts, and the nozzles receive inadequate amounts. A coolant control valve needs to be installed in the system to regulate the dump coolant flow and obtain the optimum coolant split at each of the intermediate thrusts.

Conclusions resulting from Expander Cycle Engine Studies

The following conclusions are drawn from the intermediate thrust operation studies for the OTV expander cycle engine. Refer also to Table 8, a summary of all engine modifications required.

1. At 13 percent of design thrust, the engine must be kitted for operation at an engine mixture ratio above 4.35. Otherwise, fuel pump discharge flow and pressure instability for the baseline expander engine cycle will result.

TABLE 8. REQUIRED EXPANDER CYCLE ENGINE MODIFICATIONS FOR INTERMEDIATE THRUST LEVEL OPERATION AT O/F MIXTURE RATIO OF 6

INTERMEDIATE THRUST LEVEL (lb)	MODIFICATIONS TO EXPANDER CYCLE ENGINE COMPONENTS	DESIGN THRUST (lb)		
		10K	15K	20K
2K	1. Main injector modification 2. Fuel pump recirculation 3. Dump coolant valve control mod	Yes	Yes	Yes
3K	1. Main injector modification 2. Fuel pump recirculation 3. Dump coolant valve control mod	Yes No Yes	Yes No Yes	Yes Yes Yes
5K	1. Main injector modification 2. Fuel pump recirculation 3. Dump coolant valve control mod	Yes No Yes	Yes No Yes	Yes No Yes
7K	1. Main injector modification 2. Fuel pump recirculation 3. Dump coolant valve control mod	Yes No Yes	Yes No Yes	Yes No Yes

2. Pump recirculation in the amount of 10 percent is recommended for the expander cycle engine to reduce pump instability. This requires the fuel pump to be kitted with a recirculation line and throttling valve.

3. Pump recirculation must be augmented with system pressure drop reduction through removal of calibrated orifice used for pressure drop margin present in full thrust 15K baseline.

4. An orifice installed in the oxidizer turbine circuit as in Fig. 11 is required to recover valve control lost in modification (3).

5. No engine pump kitting is necessary to maintain stable operating conditions at 6:1 mixture ratio and thrust fractions above 13% of design.

6. Main injector modification is required at all intermediate thrust levels to obtain maximum combustion efficiency at all thrust levels. Modification consists of re-orificing of injector fuel sleeve, accessible from the injector face.

7. Dump-coolant valve control needs modification to provide coolant-flow split control between combustor, fixed nozzle and dump-cooled nozzle,at each of the intermediate thrusts.

IMPACT ON ENGINE DESIGN - STAGED COMBUSTION CYCLE

The OTV engine digital computer transient model was used to investigate the staged combustion cycle engine operation at low thrust conditions. Pump operation and feed system stability were examined using the OTV engine start transient model. Heat exchanger flowrate requirements and thrust chamber

coolant flowrate requirements were established analytically. LOX injector flowrate requirements for stability and thrust chamber combustion performance were assessed using ASE and J-2S test experience.

The objectives of the OTV computer model runs (Table 9) were to establish steady state chamber pressure and mixture ratio conditions, valve scheduling and valve positioning required for efficient combustion of propellants in the chamber and for stable system operation. The pumps were assumed to be thermally conditioned to saturated propellant temperatures of 162.7 R for the LOX and 37.8 R for the hydrogen. Propellants were made available at tank pressure corresponding to NPSH values of 2 and 15 for the oxygen and hydrogen, respectively, at temperatures prescribed above. These conditions are all based on an assumed period of tank head idle mode operation prior to the low thrust operation.

TABLE 9. SYSTEM OPERATION AND STABILITY, OBJECTIVES AND CONDITIONS - OTV START MODEL RUNS

OBJECTIVES

- Attainable steady-state chamber pressure
- Attainable steady-state mixture ratio
- Effect of main oxidizer valve (MOV) opening on mixture ratio
- Effect of pump H/Q map characteristics
- Effect of fuel pump recirculation
- Effect of turbine bypass for thrust control

OTV ENGINE START CONDITIONS

- Propellants in tanks at saturated conditions
- Pumps thermally conditioned to cryogenic temperature
- LOX heat exchanger flow and temperature modeled
- Injector/dome heat transfer not modeled

The results of the study of extended low thrust operation of the OTV staged combustion engine indicate that long life, high performance operation of the engine in the low thrust mode can be achieved with minimum modification (kitting) of the engine.

Required modifications to the high-thrust staged-combustion engines are listed below and tabulated for each intermediate thrust in Table 10.

1. For low engine thrust levels (approximately 13 percent of design thrust) removal of the preburner is required, since the engine can be operated in an expander cycle mode and a preburner would require extreme deep throttling. Removal of the preburner injector increases the available turbine inlet pressure to drive the turbines. The preburner LOX line and valve can be removed in order to simplify the system.

2. For thrust levels between 13 and 50 percent of design thrust, the preburner requires injector modification to insure adequate injector pressure drops and preburner combustion stability. Above 50 percent of design thrust, no preburner modification is required. Modification of the main injector is required to increase the fuel side velocity and in that manner offset the low oxidizer side pressure drop and attendant lower performance. As discussed previously for the expander cycle thrust chamber injector, the increased momentum exchange between fuel and oxidizer will maximize injector performance at all intermediate thrusts.

3. The use of fuel pump recirculation is required to avoid the positive slope region of the pump H-Q curve. This modification would add a line and valve from the pump discharge to the pump inlet in order to recirculate hydrogen and keep the pump at a higher

TABLE 10. REQUIRED STAGED COMBUSTION CYCLE ENGINE MODIFICATIONS FOR INTERMEDIATE THRUST LEVEL OPERATION AT O/F MIXTURE RATIO OF 6

INTERMEDIATE THRUST LEVEL (lb)	MODIFICATIONS TO STAGED COMBUSTOR CYCLE ENGINE COMPONENTS	DESIGN THRUST (lb)		
		10K	15K	20K
2K	1. Preburner injector modification	Yes	No - P/B	No - P/B
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	Yes	Yes
	4. Dump coolant valve control mod	Yes	Yes	Yes
3K	1. Preburner injector modification	Yes	Yes	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	Yes
	4. Dump coolant valve control mod	Yes	Yes	Yes
5K	1. Preburner injector modification	No	Yes	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	No
	4. Dump coolant valve control mod	Yes	Yes	Yes
7K	1. Preburner injector modification	No	No	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	No
	4. Dump coolant valve control mod	Yes	Yes	Yes

flowrate. Computer model studies have indicated that this is an effective method to avoid pump instability problems when operating at low flow conditions. The computer modeling has included the effect of propellant heating which occurs when pump recirculation is used. The resulting increased flow vapor fraction is maintained low by reducing recirculation to low values and augmenting this procedure by reducing system pressure drops as explained for the expander cycle engine.

4. To effect proper cooling of combustor, nozzle, and retractable nozzle, a modified dump-coolant control valve is used to vary coolant split at each intermediate thrust level.

The major modifications which are recommended for kitting of the OTV staged combustion engines are: (1) removal or modification of the preburner injector. (2) modification of the main injector to improve combustion efficiency, (3) the use of fuel pump recirculation to avoid fuel pump operation in the positive slop region of the H-Q map, and (4) the use of a modulated dump coolant control valve. A jet pump added in the fuel pump recirculation circuit will reduce the problems arising from high vapor fractions in the flow.

Flow instabilities were not present in the oxidizer system during simulation of low thrust operation and, based upon the computer simulation, no changes to the oxidizer pump and feed system are recommended.

20K Staged Combustion Engine Operation Near 2000 Lb.

The digital computer transient model was used to examine 20K staged combustion engine operation at thrusts near 2000 lb. Results of these studies were used to provide recommendations for other intermediate thrust levels and other design thrusts. Low thrust operation of the staged combustion engine was accomplished in a pump-idle mode where the engine was operated as a pump-fed

expander cycle system. The basic staged combustion 20K engine is shown schematically in Fig. 17. This engine resulted from the OTV Phase A contract NAS 8-32996 studies and is discussed in detail in Rocketdyne Report RI/RD79-1912-2, dated 9 July 1979. Modifications to the OTV engine to make it suitable for extended operation at low thrust are the removal of the preburner for the 200-lb-thrust level and the addition of a fuel pump recirculation line and valve.

Prior to pump-fed low-thrust operation, the engine is thermally conditioned by tank-fed idle mode operation where the pumps are inoperative and the thrust chamber is operated under tank head pressures. To initiate low thrust mode pump-fed operation from tank head idle, the pump brakes are released. Initially, sufficient energy to transition-to-pump feed idle is provided by the residual heat in the combustion chamber hardware. The hydrogen coolant is directed to the high pressure turbines since the preburner has been removed. The main oxidizer valve is opened to partially provide oxygen to the main chamber where it is combusted with the hydrogen exhausting from the main turbines. As the pumps increase speed, chamber pressure increases to the nominal steady-state value.

The computer model used to simulate the engine operation includes detailed component descriptions of the engine components. Propellant heating effects of fuel pump recirculation were accounted for in the calculation of pump available NPSH and vapor fraction in the flow. Results obtained near the 2000-lb thrust level are shown in Fig. 19. The preburner has been removed from the power cycle. The turbines are powered by the thrust-chamber-heated hydrogen. To prevent pump instability, pump recirculation is performed beyond a mixture ratio of 2:1. With the engine schematic as indicated in Fig. 17 (with parallel turbines arrangement, the maximum chamber pressure obtained with the turbine bypass valve closed is 200 psia at a mixture ratio of 2:1. The limited hydrogen coolant heating, the parallel turbine arrangement, and the system pressure drop limit the chamber pressure to 200 psia. Pump recirculation for higher mixture ratios reduces the chamber pressure capabilities by absorbing higher pump power at the fuel pump.

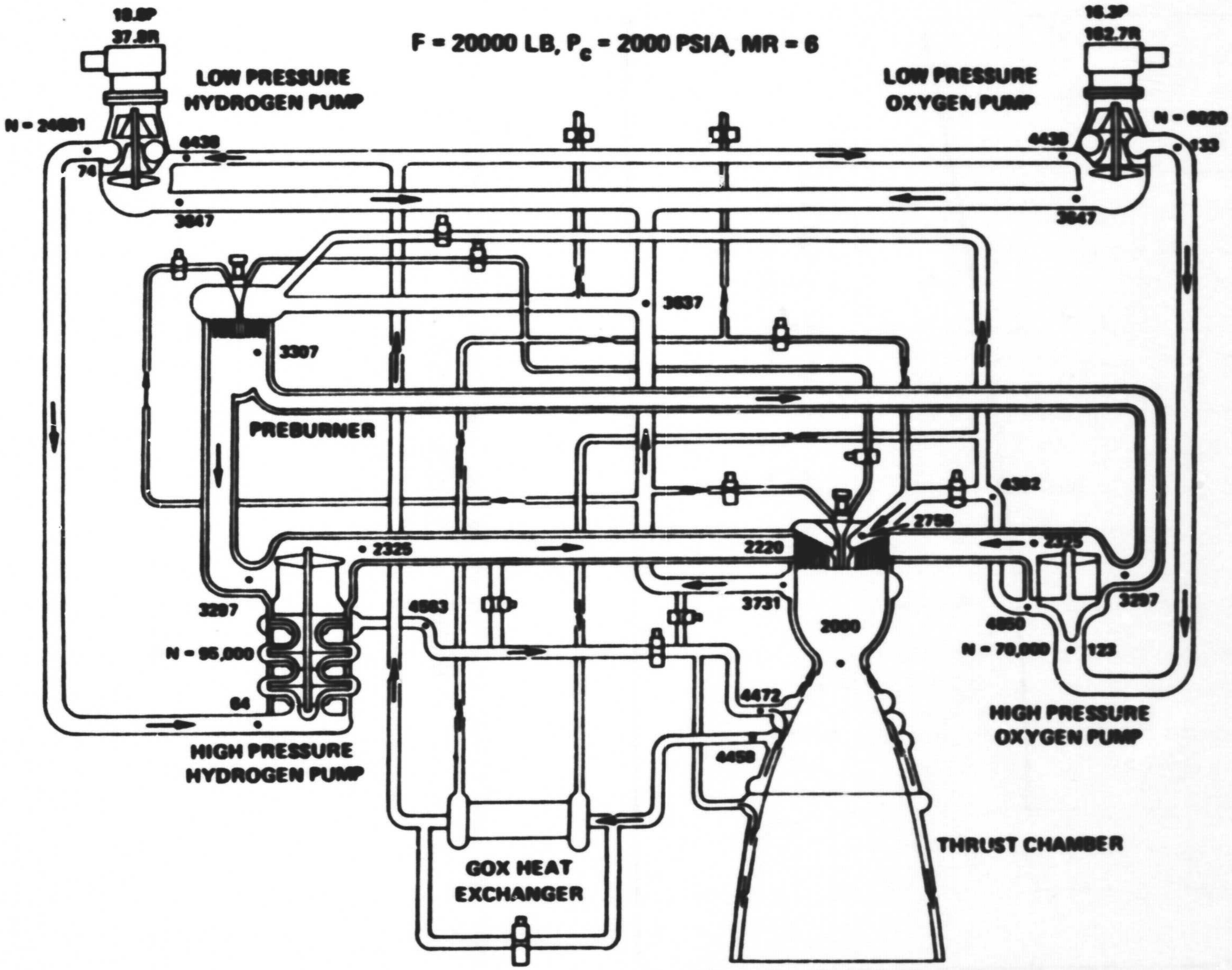


Figure 17. OTV STAGED COMBUSTION CYCLE ENGINE SCHEMATIC INITIAL PHASE A STUDIES

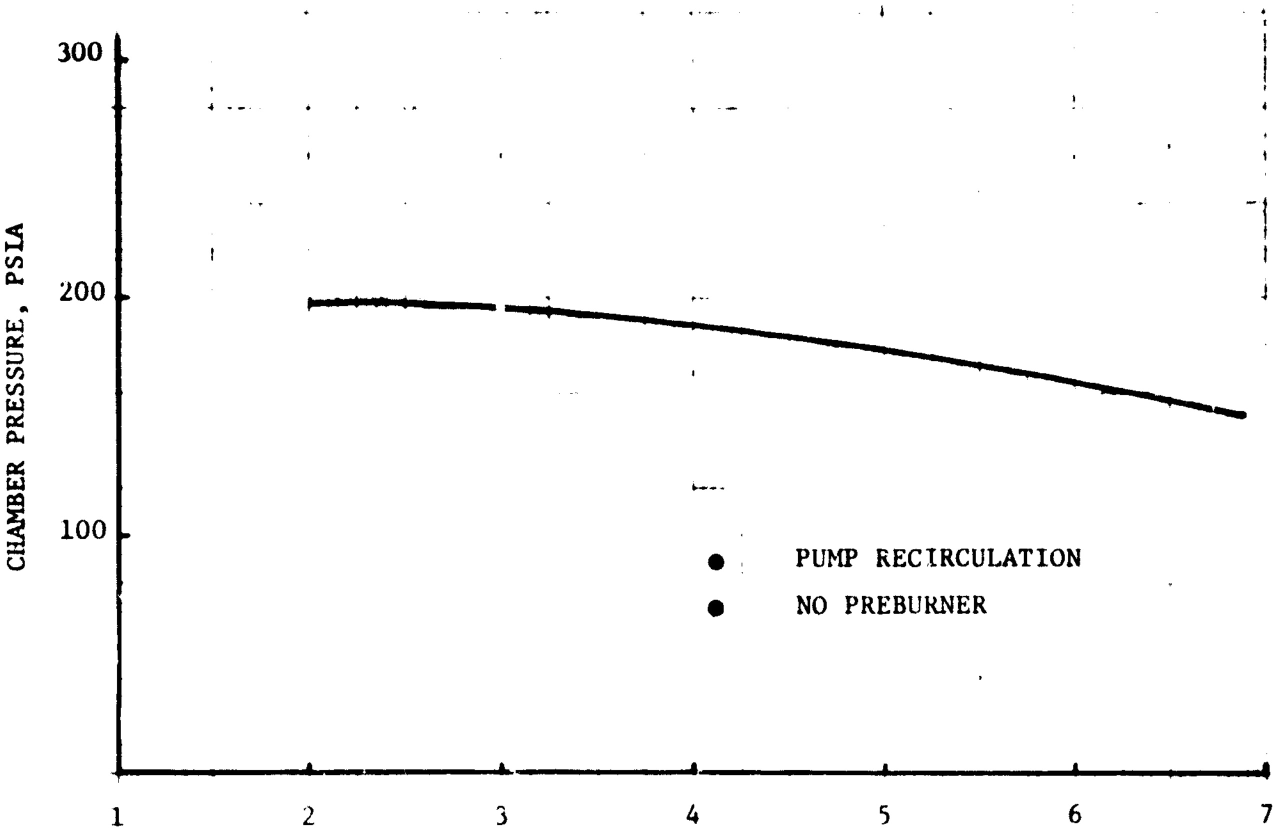


Figure 18. 20K STAGED COMBUSTION ENGINE LOW-THRUST CHAMBER PRESSURE AND MIXTURE RATIO CAPABILITIES

This expander cycle mode of operation is required at the low thrusts (near 2000) because the preburner injector throttling required is deeper than that of the thrust chamber. The deeper throttling would lead to preburner instability. The attainable thrust level can be improved through use of series turbines and reduction of system pressure drop. System pressure drop reduction is also required to allow the turbine bypass valve to open and regain some controllability of thrust.

Available NPSH to the pump and vapor conditions at the pump inlet resulting from recirculation are indicated in Table 11, a list of system parameters for two mixture ratio runs bracketing the desired mixture ratio of 6:1. For the existing NPSH conditions at the oxidizer and fuel tanks, 2 feet and 15 feet, respectively, the result of main fuel pump recirculation is to create a 5 - 30 percent vapor (by volume) condition at the pump inlet (Table 11). The vapor fraction can be reduced by using a jet pump, or performing the recirculation between higher pressure stages. Reduction of system pressure drop will also reduce the amount of recirculation required and thus the vapor fraction.

PROGRAMMATICS FOR INTERMEDIATE THRUST OPERATION

The cost and schedule impacts of kitting OTV engines for intermediate thrusts were determined as increments with respect to cost and schedule information presented in RI/RD 79-191-2, Vol. II-B (20K lb thrust staged combustion engine) and in RI/RD 80-155-2, Vol. II-B (10K lb thrust expander engine). Table 12 shows the required engine modifications to enable intermediate thrust operation (2K to 7K lb thrust) of the expander cycle and staged combustion cycle engines. The baseline engines are designed for rated thrust levels of 10K, 15K and 20K lb. All engines require kitting; as a minimum, a main injector modification (consisting of installing a different rigimesh plate retainer nut into the LOX post

TABLE 11. 20K STAGED COMBUSTION ENGINE PERFORMANCE AT LOW THRUST WITH PUMP RECIRCULATION

	MR = 5.82	MR = 6.5
Thrust (lbf)	1640	1610
Main Chamber Pressure (lb/in.^{2})	178.2	154.3
Specific Impulse (sec)	458.0	451.0
Pump		
Main Pump Speed (rpm)		
Fuel	19677	17961
Oxidizer	17766	16243
Boost Pump Speed (rpm)		
Fuel	13640	12720
Oxidizer	3442	3091
Main Pump Discharge Press. (psia)		
Fuel	289.9	245
Oxidizer	377.4	314.3
Boost Pump Discharge Press. (psia)		
Fuel	30.81	29.27
Oxidizer	37.7	33.72
Main Pump Flowrate (lb/sec)		
Fuel	.8657	.7911
Oxidizer	3.208	2.904
Preburner		
Gas Temp, R	617.2	661.9
Preburner Pr (psia)	212.8	182.4
Turbine		
Main Turbine Flowrate (lb/sec)		
Fuel	.339	.139
Oxidizer	.172	.274

TABLE 11. 20K STAGED COMBUSTION ENGINE PERFORMANCE AT LOW THRUST WITH PUMP RECIRCULATION (continued)

	MR = 5.82	MR = 6.5
Turbine		
Boost Turbine Flowrate (lb/sec)		
Fuel	.0308	.0248
Oxidizer	.0257	.0206
Valves		
MOV (% open)	56	56
Fuel Pump Feedback Valve		
ΔP across Valve (psia)	259.1	215.7
Flowrate thru Valve (lb/sec)	.315	.346
Turbine Bypass Valve	Closed	Closed
Suction Conditions		
NPSP (ft)	0	0
Fuel Tank Pressure, psia	19	19
Oxidizer Tank Pressure, psia	17	17
Vapor Fraction in H_2 Flow, % Vol.	5.6	30.2

ends) and a fuel dump valve control modification (increased valve movement steps) are needed.

All cost estimates are based on the following groundrules:

1. The intermediate thrust engine development is assumed to be preceded by the full development of a rated high thrust engine. The intermediate thrust engine development therefore will build on the basis of a rated thrust engine whose operating characteristics have been firmly established by detailed component and engine tests.

TABLE 12. REQUIRED ENGINE MODIFICATIONS FOR INTERMEDIATE THRUST LEVEL OPERATION AT O/F MIXTURE RATIO OF 6

INTERMEDIATE THRUST LEVEL (lb)	MODIFICATIONS TO EXPANDER AND STAGED COMBUSTOR (Exception: Item 1 for SC only)	DESIGN THRUST (lb)		
		10K	15K	20K
2K	1. Preburner injector mod (SC only)	Yes	No - P/B*	No - P/B
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	Yes	Yes
	4. Dump coolant valve control mod	Yes	Yes	Yes
3K	1. Preburner injector mod (SC only)	Yes	Yes	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	Yes
	4. Dump coolant valve control mod	Yes	Yes	Yes
5K	1. Preburner injector mod (SC only)	No	Yes	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	No
	4. Dump coolant valve control mod	Yes	Yes	Yes
7K	1. Preburner injector mod (SC only)	No	No	Yes
	2. Main injector modification	Yes	Yes	Yes
	3. Fuel pump recirculation	No	No	No
	4. Dump coolant valve control mod	Yes	Yes	Yes

* P/B = Preburner

2. Engine conversion from rated to intermediate thrust is performed by modifying (kitting) existing engine components as detailed in Table 12.

3. The intermediate thrust engine will be tested following near completion of all rated thrust engine system development tests.

The OTV main engine incremental ROM costs for intermediate thrust level kitting were determined for each WBS element of the 20K lb staged combustion engine, and of the 10K lb expander engine. For all other rated thrust levels the DDT&E, production and operation costs were interpolated using the two detailed cost estimates as reference points. Table 13 shows the ROM incremental cost estimate breakdown by WBS element for the 20K lb staged combustor and the 10K lb expander. Major DDT&E costs are incurred due to the substantial amount of required engine system testing (300 tests). This cost item is independent of engine cycle or thrust level. The production cost increment for a staged combustion engine kitted for intermediate thrust is practically zero when the preburner is eliminated, since preburner deletion cost savings (indicated in brackets) offset the other engine modification costs.

Table 14 is a summary of the total DDT&E, production and operations delta costs for both staged combustor and expander, with design thrust levels of 10K, 15K, and 20K lb, for four intermediate thrust levels. Depending on the intermediate thrust level, the DDT&E delta costs are in the range of about 10 to 13 $\overline{M}$. Production first unit delta cost changes range from about 2 K$ decrease to 64 K$ increase, depending on engine cycle and thrust level. Operational cost increases are estimated to about 16 K$ per engine per year, independent of thrust level or engine cycle.

The schedule for intermediate thrust engine availability is based on the groundrule that the development program for the rated OTV engine precedes that of the intermediate thrust engine. Under this assumption, the pacing schedule item is

TABLE 13. ROM DELTA COSTS FOR ENGINE KITTING
(in FY 1979 $)

WBS ELEMENT		20K LB THRUST STAGED COMBUSTOR	10K LB THRUST EXPANDER
DDT&E			
1.1.1.1	Main Fuel Pump	2.25 M	2.19 M
1.1.2.1	Main Combustion Chamber Injector	1.05 M	1.02 M
1.1.3.1	Preburner Injector	0.50 M	-
1.1.5.2	Control Valves	0.65 M	0.63 M
1.1.10	Engine Tests	7.8 M	7.8 M
1.1.11	System Engineering & Integration	0.8 M	0.8 M
1.1.12	Project Management	0.4 M	0.4 M
PRODUCTION (per engine)			
1.2.1.2	Combustion Devices	(47.7)[1] K	10.8 K
1.2.1.3	Controls	10.2 K	9.4 K
1.2.1.6	Engine Assy, Initial Accept. Testing	23.5 K	21.8 K
1.2.4	Sustaining Engineering	9.3 K	8.6 K
1.2.5	Project Management	3.0 K	2.8 K
OPERATIONS			
1.3	Operations (per engine, per year)	16.0 K	16.0 K

Note:
1 Without preburner
With preburner modification: 18.1 K

TABLE 14. SUMMARY OF DELTA ROM COSTS FOR ENGINE KITTING
(in FY 1979 $)

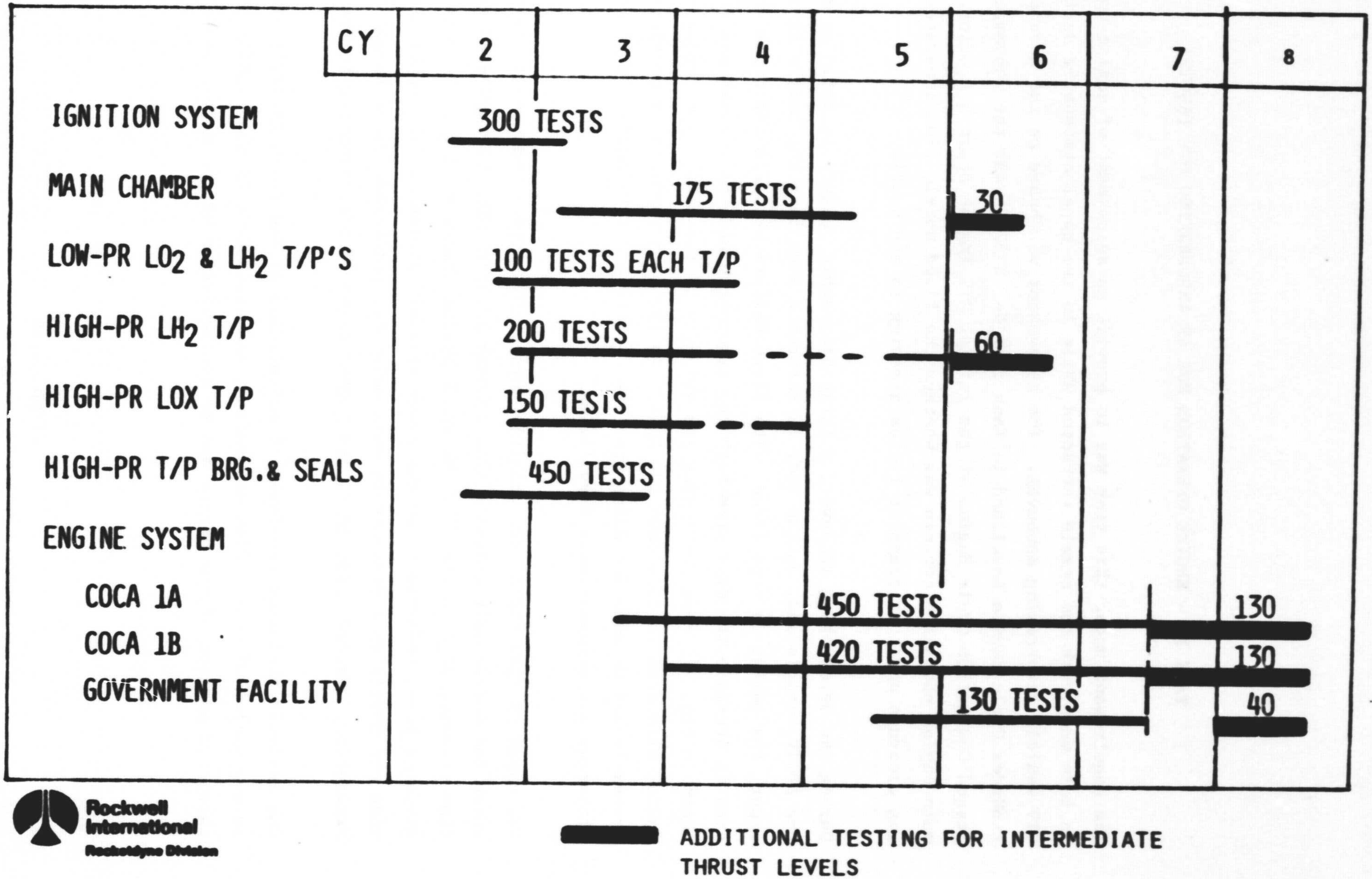
INTERMEDIATE THRUST LEVEL (lb)	COST ITEM	DESIGN THRUST (lb)					
		10K		15K		20K	
		SC	EXP	SC	EXP	SC	EXP
2K	DDT&E (M$)	10.7	10.2	12.9	12.9	12.95	12.95
	Production (K$)	57.7	47.5	(1.7)	57.7	(1.7)	57.7
	Operations (K$/yr)	16.0	16.0	16.0	16.0	16.0	16.0
3K	DDT&E (M$)	10.7	10.2	10.7	10.2	13.45	12.95
	Production (K$)	57.7	47.5	57.7	51.3	64.1	57.7
	Operations (K$/yr)	16.0	16.0	16.0	16.0	16.0	16.0
5K	DDT&E (M$)	10.2	10.2	10.7	10.2	10.75	10.25
	Production (K$)	51.3	47.5	57.7	51.3	57.7	51.3
	Operations (K$/yr)	16.0	16.0	16.0	16.0	16.0	16.0
7K	DDT&E (M$)	10.2	10.2	10.2	10.2	10.75	10.25
	Production (K$)	51.3	47.5	51.3	51.3	57.7	51.3
	Operations (K$/yr)	16.0	16.0	16.0	16.0	16.0	16.0

NOTE: 1. Production and Operation costs are on a per-engine basis.

2. Only 10K lb thrust expander and 20K lb thrust staged combustor delta costs were determined by WBS element. Delta costs for all other thrust levels were interpolated.

3. Numbers in brackets are cost savings.

engine system testing, as shown in Fig. 20 . This figure shows a typical OTV expander engine development test schedule. The schedule is independent of the rated thrust level in the 10 to 20 K lb range and is very similar to that of a staged combustion cycle engine. Out of 300 required additional engine system tests for intermediate thrust kitting, 130 can be performed on two Rocketdyne test stands each (COCA 1A and 1B) following the end of the rated thrust engine system test program. Forty tests need to be performed at altitude conditions; these tests will be run at a government test facility. With the test schedule shown in Fig. 20, the initial operational capability (IOC) of an intermediate thrust expander or staged/combustor OTV engine is 13 months following the IOC date of the rated thrust OTV engine.



Rockwell International
Rocketdyne Division

Figure 19. 10K Expander Engine Major Development Testing

TASK 16 - ENGINE OPERATION FOR AN AEROBRAKING OTV (ABOTV)

The requirement under this task was to provide an assessment of engine operation in idle mode, with the nozzle retracted, while in the upper atmosphere during an OTV vehicle aerobraking maneuver. The assessment was based on the Advanced Expander Cycle Engine baselined in Task 8 of NAS 8-32996 and the recommended Staged Combustion Cycle Engine of the initial OTV Engine Phase A studies. The main engine operation concern was whether the fixed nozzle flow separates, creating pressure and heat transfer fields damaging to the nozzle.

During the aerobraking maneuver, a large disposable ballute is inflated around the ABOTV vehicle and, as the body is returning such that the nozzle is in front, the engine is retrofired. The large ballute provides additional surface area to increase the drag from both the atmosphere and the exhaust gases, as shown in Fig. 21, thus decreasing the velocity of the body. The firing of the engine also acts to slow down the body. As the body reenters, a complex shock system is set up and, depending upon the operation of the engine, will result in different slipstream-nozzle flow fields.

Two types of flow fields are possible during this type of maneuver. In the case where the nozzle exit pressure (P_E) is much greater than the separated slipstream flow pressure (P_B) in Fig. 21, the shock system stands well off the nozzle, this distance being referred to as the shock standoff distance. The other case is when the nozzle exit pressure is well below P_B, which results in nozzle flow separation, unsteady sideloads, and higher rates of heat transfer to the nozzle.

The analysis performed consisted of calculation of the shock standoff distance, an evaluation of nozzle flow separation, and assessment of the heating of the retracted portion of the nozzle due to recirculation of the nozzle propellants.

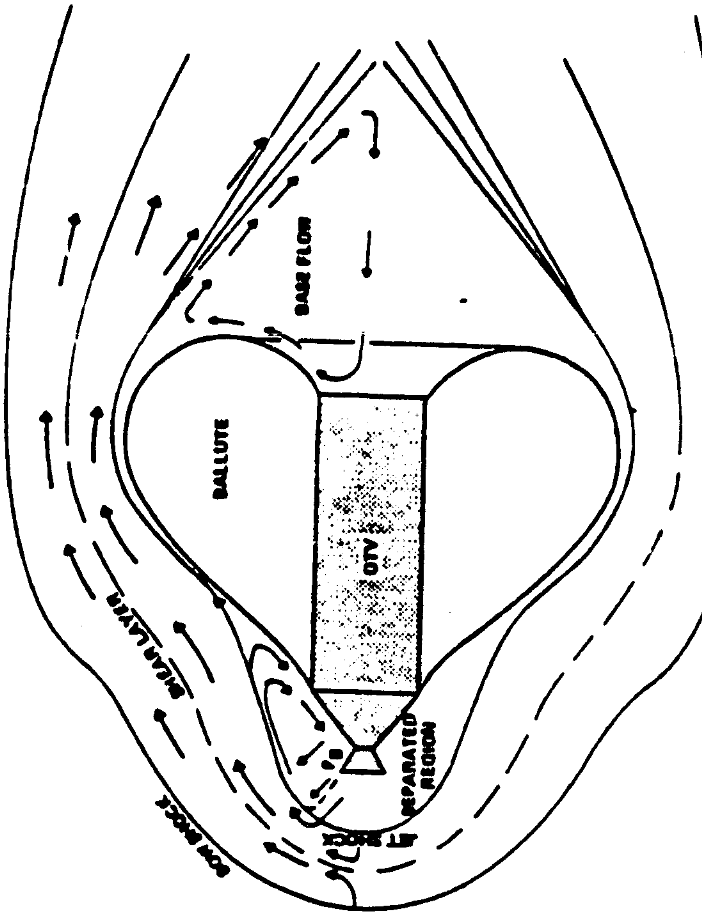


Figure 20. ABOTV Concept and Flowfield

EXPANDER CYCLE ENGINE OPERATING CONDITIONS

Table 15. presents OTV expander engine operation conditions as well as the slip-stream flow characteristics. As shown in the table, the base pressure (P_B) is dependent upon the impact pressure which is defined as follows:

$$P_{impact} = 1.84\ q$$

where q is the average dynamic pressure shown in Table 15.

TABLE 15. OPERATING CONDITIONS DURING ABOTV MANEUVER, EXPANDER CYCLE ENGINE

OPERATING MODE	P_C (PSIA)	ALTITUDE (FT)	MACH NO.	AV. DYNAMIC PRESS., PSF	P_B
H_2 (only)	7.0	400K	25	0.5	1/2 P_{impact}
Tank Lead Idle	7.0	292K	36	5.0	1/2 P_{impact}
Pumped Idle	203	265K	34	10.0	0.2 P_{impact}
Pumped Idle	203	262K	33	15.0	0.2 P_{impact}

Shock Standoff Distance

Figure 21 depicts the model assumed for calculating the shock standoff distance (L). Note the stagnation point along the dividing streamline. As the supersonic air flow approaches this stagnation point, it passes through a normal shock. Similarly the rocket nozzle flow passes through a normal shock before reaching the stagnation point. Since the stagnation point is common to both flows, the total pressure behind each normal shock must be matched.

P_{impact} is the stagnation pressure behind each shock in Fig. 21. It was obtained from the average dynamic pressure provided by the vehicle contractor and listed in Table 15. The ratio of P_{impact} to the nozzle stagnation pressure P_c and the normal shock relations are used to determine the nozzle flow Mach number, M1, and area ratio, ϵ, can be obtained. From this area ratio, the distance to the shock can be approximated by assuming source flow and using spherical areas. Table 16 presents the normal shock results along with the distance from the nozzle throat to the shock.

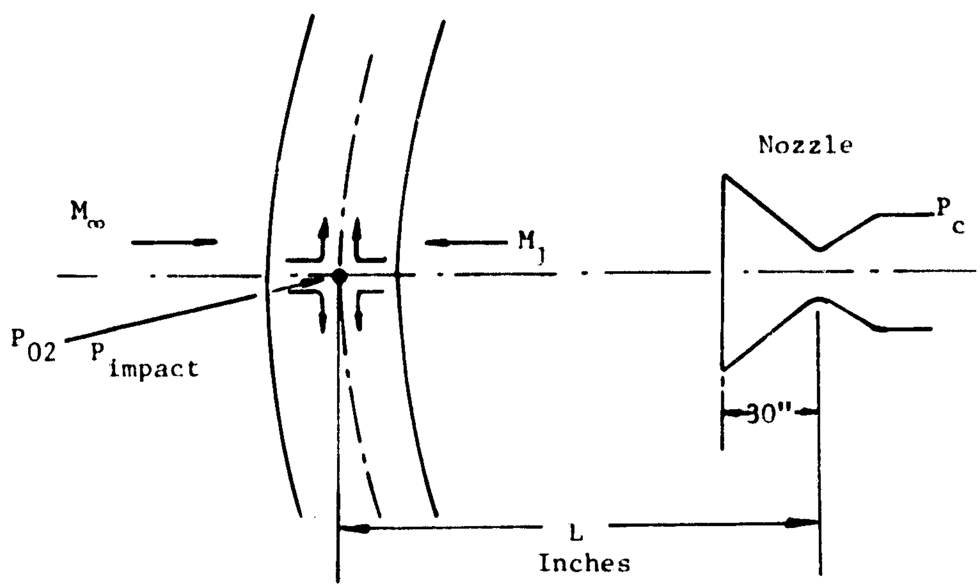


Figure 21. Flow Model of Shock System

TABLE 16. NORMAL SHOCK RESULTS

OPERATING MODE	P_c (PSF)	P_{impact} (PSF)	$\frac{P_{o_2}}{P_{o_1}}$	M_1	SHOCK STANDOFF DISTANCE (L) (IN. FROM THROAT)
H_2 (only)	1.0×10^3	0.92	9.2×10^{-4}	12.88	104
Tankhead Idle	1.0×10^3	9.2	9.2×10^{-3}	7.84	32.4
Pumped Idle	2.[illegible] $\times 10^4$	18.4	6.3×10^{-4}	13.94	126
Pumped Idle	2.92×10^4	27.6	9.45×10^{-4}	12.80	102

Note that in all cases except the tankhead idle case (THI), the shock is well downstream of the nozzle exit (30 inches from the throat). In the THI mode, the shock is very close to the nozzle exit.

In the above calculations, it was necessary to assume continuum mechanics for the nozzle flow only since all other properties were provided. This assumption was verified by using a Method of Characteristics solution of the nozzle flow field out to a Right Running Characteristic (RRC) line from the nozzle exit. The assumption is valid as long as the radius of the spherical core (L) is within this RCC.

It was assumed to obtain a solution in the case of idle mode with uncombusted hydrogen flow in the main chamber that the hydrogen is heated in the coolant jackets to a steady state temperature of 530 R by absorbing residual heat in the hardware. The hardware residual heat depends on the environmental heat transferred to the thrust chamber during reentry. A hydrogen cooling-off trend will be established upon start of the hydrogen flow in the chamber. Depending on the heat transferred to the nozzle in the slipstream-nozzle interaction flow

field, the steady state hydrogen equilibrium temperature might reach low enough levels to collapse the nozzle flow field leading to eventual separation.

Nozzle Flow Separation

In order to both check the above calculations as well as estimate nozzle sideloads, it is necessary to check each case for flow separation. The criterion used to predict whether the nozzle flow separates is

$$\frac{P_B}{P_W} \geqslant 3.0 \text{ to } 5.0$$

where P_W is the nozzle wall pressure. Once the above condition has been reached, the flow will separate from the wall and unsteady sideloads and higher rates of heat transfer to the wall will result.

Table 17 represents a summary of conditions for each of the cases analyzed. As indicated in the table, separation occurs at the THI conditions only which agrees with the previously discussed shock standoff distance results. The point at which flow separation occurs during the THI case can be estimated by using Fig. 23 and determining the point at which $\frac{P_B}{P_W} > 3.0$. This occurs at $X/R_T = 8.0$ or an expansion area ratio of $\epsilon = 53$. In order to estimate the side load amplitude, it is assumed that only half the flow separates at $\frac{P_B}{P_W} = 3.0$ while the rest separates at $\frac{P_B}{P_W} = 5.0$, as shown in Fig. 24.

Using this approach results in an asymmetric loading of the nozzle, a portion of the outer wall having a greater pressure than the inner wall. This approach results in a resultant horizontal sideload. Taking the difference between the average wall pressure over the region of loading and the ambient pressure, the side force can be estimated as:

$$\begin{aligned} F_s &= \Delta PA \\ &\sim 2.5 \text{ lbf horizontal side force.} \end{aligned}$$

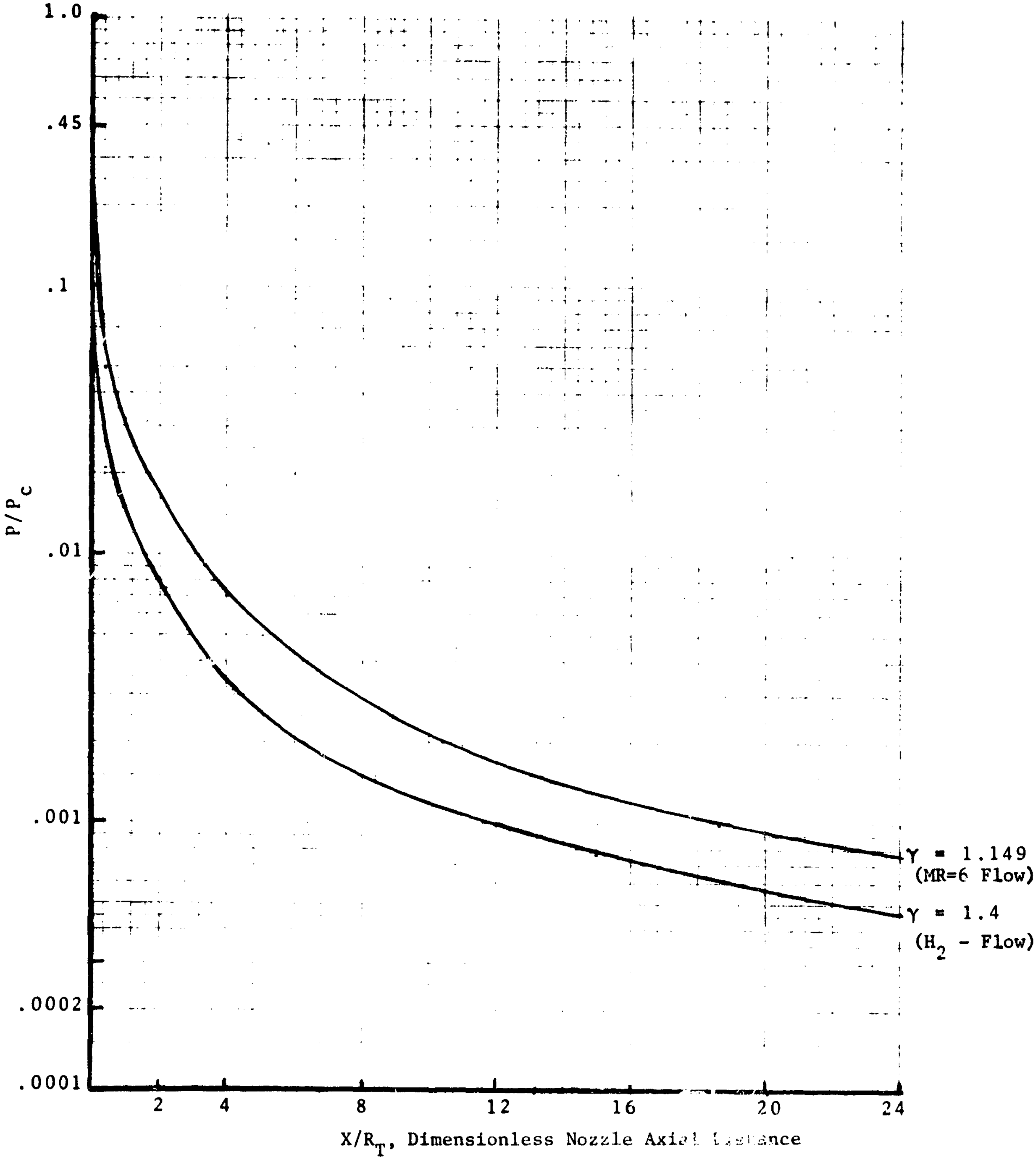


Figure 22. ABOTV Nozzle Wall Pressure Profile for Constant Specific Heat Ratios, γ = 1.149 and γ = 1.4

TABLE 17. ABOTV NOZZLE FLOW SEPARATION STUDY

OPERATING MODE	P_c (PSIA)	P_B (PSF)	P_W* (PSF)	$\frac{P_B}{P_W}$	Separated $\frac{P_B}{P_W}$ 3.0
H_2	7.0	0.46	0.45	1.02	No
Tankhead Idle	7.0	4.6	0.45	10.2	Yes
Pumped Idle	203	3.68	13.15	0.28	No
Pumped Idle	203	5.52	13.15	0.42	No

*Based on full flowing nozzle

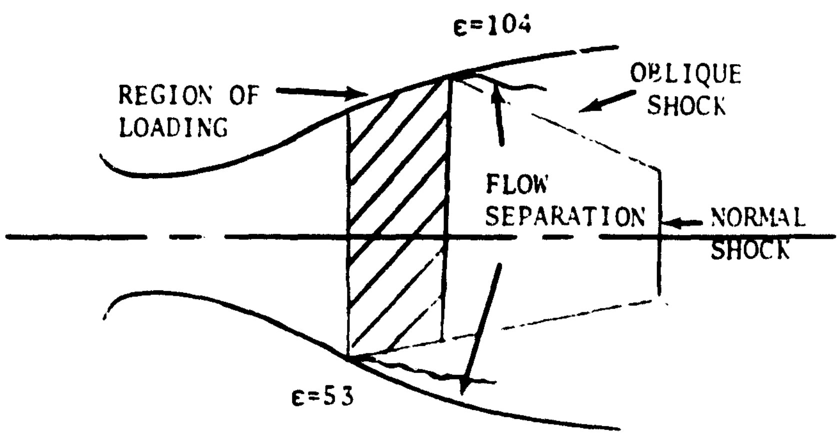


Figure 23 . Schematic Showing Side Load Mechanism

This separation can be expected to act at an area ratio of $\varepsilon = 70$, thereby providing a significant moment arm. Note that Fig. 24 helps to qualify the shock standoff distance discussed previously. Although the normal portion of the shock is standing just outside the nozzle, it is known that the upstream oblique shock will be significantly further upstream.

A large amount of side load data is available for the SSME which experiences side loads during start/stop engine transients. The maximum values have been found to be approximately 1/22 of the thrust. Using this thrust fraction as a basis and alternate procedure, a 3.3 lbf side load force is estimated for the OTV operating conditions being examined.

Nozzle Flow Separation Heat Transfer

As the flow separates during the THI mode of operation, the portion of the wall downstream of the shock will experience increased heat load due to the loss of the boundary layer. The separated heat transfer coefficient, h_{sep}, is related to the attached heat transfer coefficient h_{att} by

$$\frac{h_{sep}}{h_{att}} = \left(\frac{P_a}{P_W}\right)^{0.8} = 2.4$$

Figure 25 shows the results of the thermal analysis using the thrust chamber regenerative cooling analysis code for a chamber pressure of 7.22 psia, T_c = 4858 R, MR = 3.11, and a coolant flowrate of $\dot{w}_c$ = 0.03 lbm/sec. The figure shows a maximum wall temperature of 513 F, or approximately 140 F greater than the full flowing case. This condition should create no problem in the cooling of the nozzle.

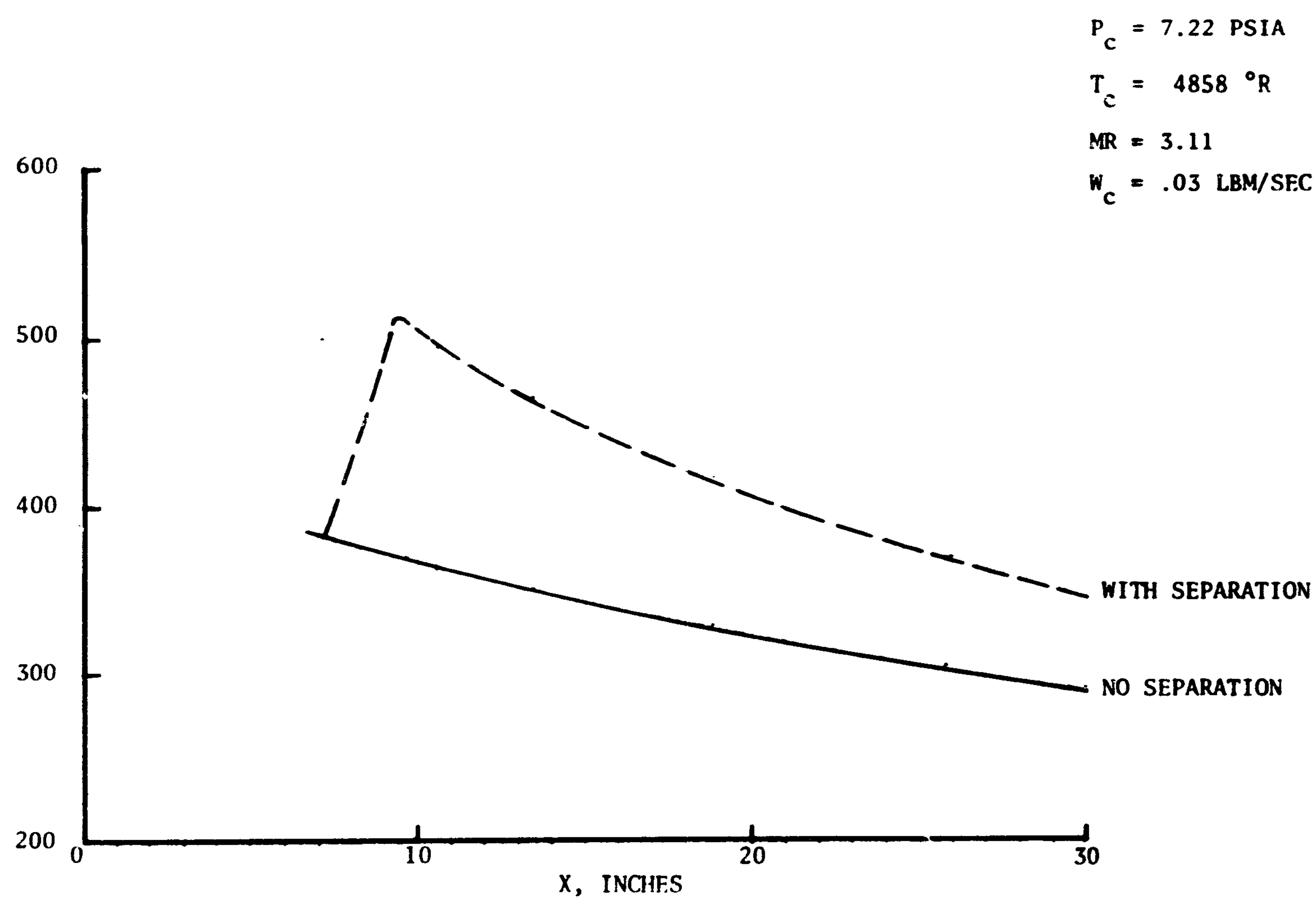


Figure 24. Comparison of Separated and Non-Separated Flow Nozzle Wall Temperatures

Heating of Retracted Nozzle Extension

During the ABOTV maneuver, the retracted portion of the nozzle experiences heating due to the recirculating exhaust gases. This area of heating has been investigated and an approximate analysis has been carried out as follows.

The exhaust for the PI mode (worst case) was expanded to P_B (one-dimensionally) to provide ρV at these conditions and a fraction of this value taken for the external heating calculation. Based upon this conserviative estimate, a dry wall temperature of 1130 F would be obtained. A more detailed analysis of the recirculation flow and resultant heating should be carried out; however, the above temperature is conservative and, based on the assumptions made, the actual wall temperature would be lower.

Conclusions

The analysis showed that for the given operating conditions the shock standoff distance is well downstream of the nozzle exit in all cases except for the tank-head idle case. In this case it was shown that the flow actually separates from the nozzle wall. The analysis showed a side load of approximately 5 lbf and an increase in wall temperature of 140 F could be expected to result from this separation. As noted earlier, the side load acting on the nozzle could result in possibly significant moments due to the distance from the throat at which the force acts.

The abbreviated analysis carried out to estimate the effects due to hot exhaust gas recirculation near the retracted portion of the nozzle indicated a conservative wall temperature of 1130 F. However, it should be noted that recirculation heating is present.

STAGED COMBUSTION ENGINE OPERATING CONDITIONS

The operating conditions for the staged combustion cycle engine recommended in NAS 8-32996 initial Phase A studies are indicated in Table 18.

TABLE 18. OPERATING CONDITIONS DURING ABOTV MANEUVER, STAGED COMBUSTION CYCLE ENGINE

OPERATING MODE	P_c (PSIA)	ALTITUDE (FT)	MACH NO.	AVE. DYNAMICS PRESSURE (PSF)	BASE PRESSURE P_B
H_2 Only	9.7	400K	25	0.5	1/2 P_{impact}
Tank Head Idle	9.7	292K	36	5.0	1/2 P_{impact}
Pumped Idle	209	265K	34	10.0	0.2 P_{impact}
Pumped Idle	209	262K	33	15.0	0.2 P_{impact}

An analysis similar to that of the expander cycle for calculating the shock standoff distance was carried out for the staged combustor cycle. The difference in chamber pressures and nozzle contour warranted the analysis. Table 19 presents the results of the analysis.

As can be seen from the table, the shock standoff distance is increased for all cases. During the expander cycle mode of operation, the tankhead idle experienced flow separation. A similar analysis was carried out for the staged combustion cycle. The results of these calculations are shown in Table 20. Note that no flow separation is predicted for this cycle. This is due to both the higher chamber pressure as well as the lower expansion area ratio, resulting in higher wall pressures. However, it should be noted that the tankhead idle case may be close to separating since the exit pressure ratio is close to the

separation pressure ratio.

TABLE 19. RESULTS OF STAGED COMBUSTION CYCLE SHOCK STANDOFF DISTANCE

OPERATING MODE	Pc (PSIA)	$\frac{P_{O_2}}{P_{O_1}}$	M_1	R (INCHES)
H_2	9.7	6.58×10^{-4}	13.83	145
Tankhead Idle	9.7	6.58×10^{-3}	8.47	45.8
Pumped Idle	209	6.11×10^{-4}	14.01	150
Pumped Idle	209	9.17×10^{-4}	12.85	121

TABLE 20. NOZZLE FLOW SEPARATION RESULTS FOR STAGED COMPUSTION CYCLE

OPERATING MODE	P_B (PSF)	P_W* (PSF)	$\frac{P_B}{P_W}$	SEPARATED $\left(\frac{P_B}{P_W} \geq 3.0\right)$
H_2	.46	1.75	.263	No
Tankhead idle	4.6	1.75	2.63	No
Pumped Idle	3.68	37.75	0.097	No
Pumped Idle	5.52	37.75	0.146	No

REFERENCES

Ref. 1 OTV Concept Definition Study NAS8-33533- Midterm Review, General Dynamics Convair Division, 23 Jan. 1980.

Ref. 2 Orbit Transfer Vehicle Engine Study, Phase A, Final Report, Vol. II-B: Cost, RI/RD790191-2, 9 July 1979; and RI/RD80-155-2, Phase A Extension Final Report, 30 June 1980.

APPENDIX A. OTV PUMP NPSH CAPABILITIES

The original suction requirements that the OTV boost pumps were designed to accommodate are summarized in Table 7-26. The Net Positive Suction Head (NPSH) values are finite and positive, and the boost pumps will operate at these values without vapor in the flow.

TABLE 7-26. ORIGINAL OTV SUCTION REQUIREMENTS

Propellant	H_2	O_2
Tank Vapor Pressure, psia	18.4	15.6
Pump Inlet NPSH, Ft.	15	2

This means that the pump inlets were sized so that the NPSH's supplied at the pump inlets are equal to, or greater than, the inlet flow velocity head. However, it is also true that pumps designed to this criteron will often operate satisfactorily at zero tank NPSH, which is a two-phase (vapor-liquid) operating condition at the pump inlet (Ref.7-3,7-6,7-7,& 7-8. This is an advantage because it eliminates the constraint of having to pressurize the tanks. Therefore, an analysis was conducted to determine the tank vapor pressures under which those boost pumps would operate at zero tank NPSH.

Because unpressurized tanks are the ultimate goal of zero tank NPSH, the analysis must begin with saturated liquid in the tanks (which is zero tank NPSH by definition), and must include a treatment of the vehicle accelerations, the inlet line flow accelerations, and the inlet line losses. The assumed inlet line geometries are summarized in Table 7-27 and the method for calculating inlet line pressure drops is summarized in Table 7-28. It must be noted that this inlet line analysis is approximate because (1) the geometries in Table 7-27 are a function of the vehicle, which is not fully defined at this time, and (2) as implied in Table 7-28 , the pressure drop calculations were

simplified considerably by using constant values of liquid density, which produces results that are slightly optimistic. However, the results are close enough to give a good evaluation of potential capabilities.

For the existing preliminary designs of the OTV boost pumps, the pump inlet geometries, and the operating conditions at both full thrust and pump idle modes, are summarized in Table 7-29. This zero NPSH evaluation was conducted for both operating modes, because both are contractual requirements. As discussed in detail below, the results of the analysis indicate that the designs will operate at zero tank NPSH with little or no modifications if the tank vapor pressures are equal to, or greater than, those shown in Table 7-26. If the tank vapor pressures are reduced to a very low value of 10 psia, relatively minor design modifications would be required. These modifications are: (1) on the LOX side, a 20% increase in inlet line and boost pump inlet tip diameters in order to accommodate full thrust operation, and (2) on the LH_2 side, a 25% decrease in rotational speed when operating at pump idle mode. No modifications are required to operate the LH_2 boost pump at full thrust, or the LOX boost pump at pump idle mode.

TABLE 7-27. FEED LINE GEOMETRIES

Propellant	H_2	O_2
Line Lengths, Ft.	18-20	3-4
Acceleration, G's (Full Thrust)	.1-.2	.1-.2

TABLE 7-28. ASSUMPTIONS FOR PREDICTING PUMP INLET CONDITIONS

Line Diameter = Pump inlet tip diameter

Line Losses:

One bell mouthed inlet, K_L = .04

Two vaned mitered elbows, K_L = .2/elbow

Pipe friction

Acceleration:

Flow accelerates to operating point in 5 sec.

Entire line length is in direction of motion

Pressure Drop:

$$C_m = \frac{\dot{w}}{\rho A}$$

$$\Delta P = \frac{\rho}{144}\left[\frac{C_m^{\,2}}{2g}(1 + K_L) + L\left(\frac{a_L}{g} - \frac{a_V}{g}\right)\right]$$

TABLE 7-29. OTV BOOST PUMP OPERATING CONDITIONS

Operating Mode	Parameter	H_2	O_2
All	D_{1T}, In.	3.14	3.6
	D_{1R}, In.	1.57	1.25
Full Thrust	$\dot{w}$, Lb/Sec	4.45	26.71
	N, RPM	28,490	6475
Idle	$\dot{w}$, Lb/Sec	0.76	3.02
	N, RPM	11,780	784

Cavitation Evaluation

Because operation at zero tank NPSH usually results in two-phase flow at the pump inlet, the first condition to assess is the effect of cavitation when these operating conditions are being approached. If cavitation effects are negligible, the analysis need only consider two-phase flow. However, if cavitation effects are considerable under some conditions, the analysis must also consider cavitation effects on pumping capability.

The impact of cavitation was assessed by predicting the NPSH required at 2% loss in head due to cavitation. This is done by deducting the thermodynamic suppression head (TSH) from the NPSH in cold water ($NPSH_{H_2O}$), as shown in Equation 1:

EQUATION 1

$$NPSH = NPSH_{H_2O} - TSH$$

The NPSH in water is the NPSH required at 2% head loss in water and the TSH is the correction in the subject propellant that, when deducted from the water NPSH, produces the NPSH that would give the same vapor cavity size and, consequently, the same head loss in the subject propellant (References 4, 5, and 6). Based on correlations made at Rocketdyne, the TSH's are expressed by Equations 2 and 3 for LH_2 and LO_2, respectively.

EQUATION 2

$$TSH_{LH_2} = \frac{.000691\ (d/Z)^{.16}\ U^{.85}\ \tan^2\beta(\beta_{HYDROGEN})}{\phi^4}$$

EQUATION 3

$$TSH_{LO_2} = .0986\ (d/Z)^{.16}\ U^{.85}\ \beta_{LO_2}$$

Where:

d	=	inducer inlet tip diameter, in.
Z	=	number of inducer blades at inlet
U	=	inducer inlet tip speed, ft/sec
β	=	inducer blade angle at inlet tip, degrees
ϕ	=	inducer inlet tip flow coefficient, C_m/U
C_m	=	flow axial velocity entering inducer, ft/sec
$\beta_{HYDROGEN}$	=	thermal factor for liquid hydrogen
β_{LO_2},	=	thermal factor for liquid oxygen

The two thermal factors, $\beta_{HYDROGEN}$ and β_{LO_2}, are purely a function of the propellant physical properties and are shown in Fig. 7-15as a function of propellant vapor pressure. These NPSH's were calculated for the operating conditions shown in Table 7-29 at both the design vapor pressures (Table 7-26) and at the low vapor pressures of 10 psia. The results are summarized in Table 7-30. In all cases, the thermodynamic suppression head is considerably greater than the NPSH capability in water, which means that the inducer vapor cavities are small under the anticipated flow conditions. Therefore, cavitation would have little impact on the performances of these pumps under these operating conditions. As a result, cavitation limits can be ignored and the pumping limits and capabilities are governed primarily by two-phase flow phenomena.

Two-Phase Evaluation

The two-phase flow pumping capability was evaluated by first estimating the flow conditions at the pump inlet, and then comparing those flow conditions with the flow conditions at the two-phase pumping limits. These activities are discussed separately below.

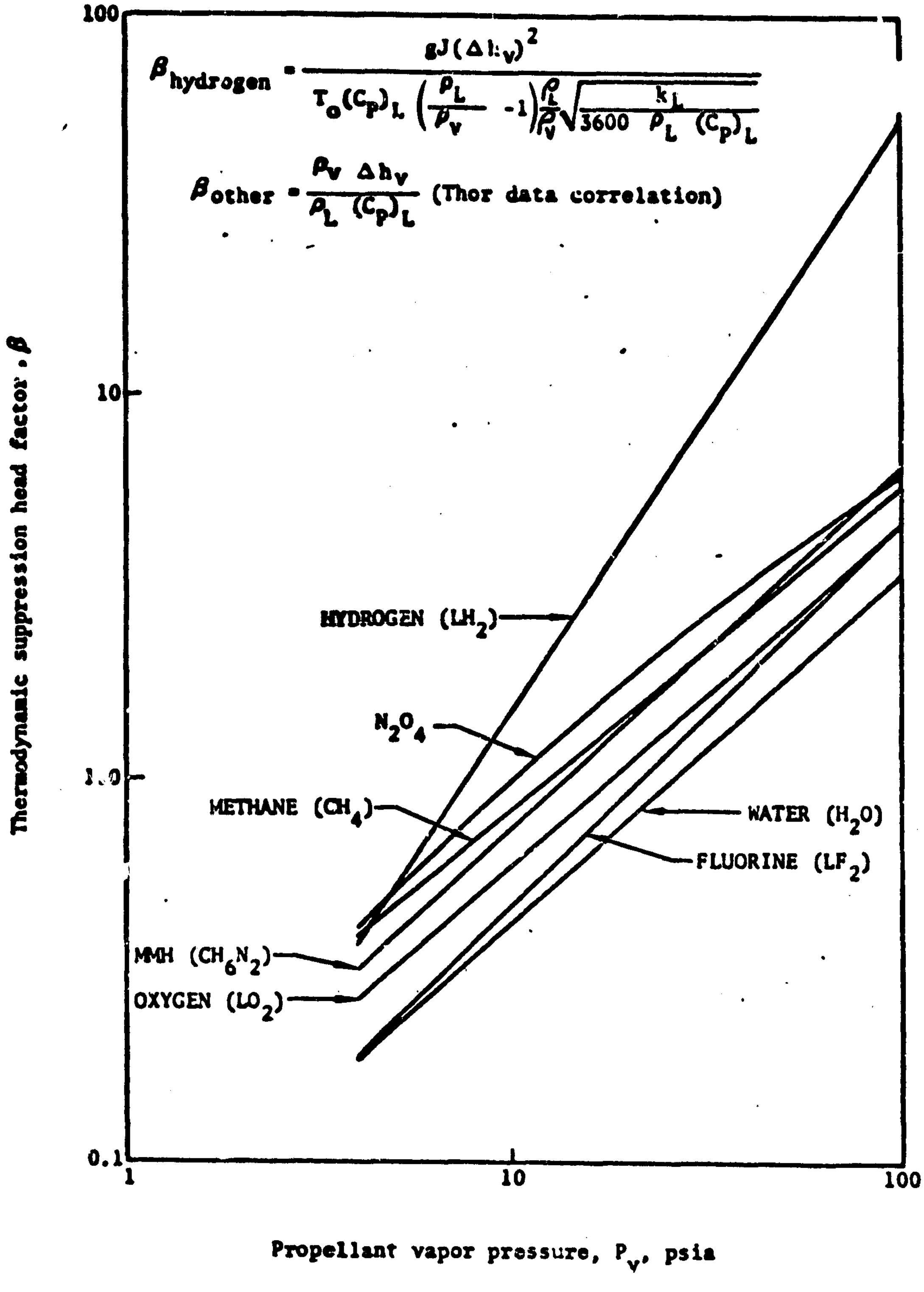


Figure 7-15. Thermodynamic Suppression Head Factor for Various Propellants as a Function of Vapor Pressure

TABLE 7-30. THERMODYNAMIC SUPPRESSION HEAD EFFECTS

Operating Mode	Fluid	Tank P_v, psia	NPSH Capability In H_2O, Ft.	TSH, Ft.	NPSH Capability In Fluid, Ft
Full Flow	H_2	18.4	2.03	4.44	-2.41
	H_2	10	2.01	2.96	- .95
	O_2	15.6	31.3	363	-332
	O_2	10	30.7	117.3	-127
P[illegible] Idle	H_2	18.4	.029	.737	-.71
	H_2	10	.029	.492	-.46
	O_2	15.6	5.22	5850	-5840
	O_2	10	5.29	2570	-2560

Pump Inlet Conditions. As discussed earlier, the inlet line assumptions used in predicting the pump inlet conditions are summarized in Table 7-28. The first one was used to relate the line velocity to the pump inlet velocity. It is reasonable because (1) the line size should be minimized in order to minimize weight, and (2) the line cross sectional area should be at least as large as the pump inlet area in order to minimize losses and (in two phase flow) avoid choking.

Also shown in Table 7-28 is that the line losses are the sum of the line component and the friction losses. With the inlet line geometries indicated in Tables 7-27, 7-28 and 7-29, the total inlet line loss coefficients are 1.74 in hydrogen and .67 in oxygen.

Two accelerations are involved in the calculations, the flow acceleration relative to the vehicle (which drops the pump inlet pressure) and the vehicle acceleration itself (which tends to increase the pump inlet pressure). The first acceleration assumption was determined from typical start transients predicted for the OTV engine. The second acceleration assumption indicates the amount of the inlet line that the vehicle acceleration acts on. The vehicle acceleration itself was assumed to be .1 G (minimum was selected from Table 7-27 in order to be conservative) for full thrust operation, which was ratioed down by the flowrates in order to get the pump idle mode value of .0121 G.

Finally, the indicated equations were used to estimate the inlet line pressure drop. Liquid flow density was assumed in order to simplify the calculation. This is somewhat optimistic because, for a given weight flowrate, the inlet line pressure drop would be (roughly) inversely proportional to average flow density.

As indicated in Table 7-28, the four components of inlet line pressure drop are the initial expansion to the line velocity, the inlet line losses, the value that accelerates the flow in the inlet line, and the pressure head value caused by vehicle acceleration. The first three decrease the pressure, and

the last increase it. These four components were assumed to be additive because the constant entropy lines on the temperature-entropy charts are almost parallel to the constant enthalpy lines at low vapor weight fractions. As a result, a pressure drop along a constant enthalpy line (which represents a loss) results in about the same amount of vapor as the same pressure drop along a constant entropy line (which represents an ideal expansion to a finite velocity). As a result, very little error is introduced by adding the pressure drops and determining the vapor fraction along a constant entropy line. This greatly simplifies the calculation.

This procedure was used to estimate the inlet line pressure drops. The liquid flow densities were obtained from Ref. 7-4 and 7-5 and are shown as a function of vapor pressure in Fig. 7-16. The line pressure drop and the differential change of quality with pressure at constant entropy (Fig.7-17, derived from Ref. 7-4 and 7-5 were used in Equation 4 to estimate the vapor weight fraction at the pump inlet.

EQUATION 4

$$X = \left[- \left(\frac{\partial X}{\partial P} \right)_s \right] \Delta P$$

This and the vapor to liquid density ratio (Fig.7-18) were then used to estimate the vapor volume fraction at the pump inlet (Equation 5).

EQUATION 5

$$\alpha = \frac{1}{\frac{\rho_v}{\rho_1}\left(\frac{1}{X} - 1\right) + 1}$$

The resulting pump inlet flow conditions (static pressure, vapor volume fraction supplied, and flow velocity) are summarized in Tables 7-31 and 7-32 at the full thrust and the pump idle modes, respectively.

Two-Phase Limits. The parameters that permit the evaluation of the two phase pumping limits were then determined to indicate the feasibility of the predicted

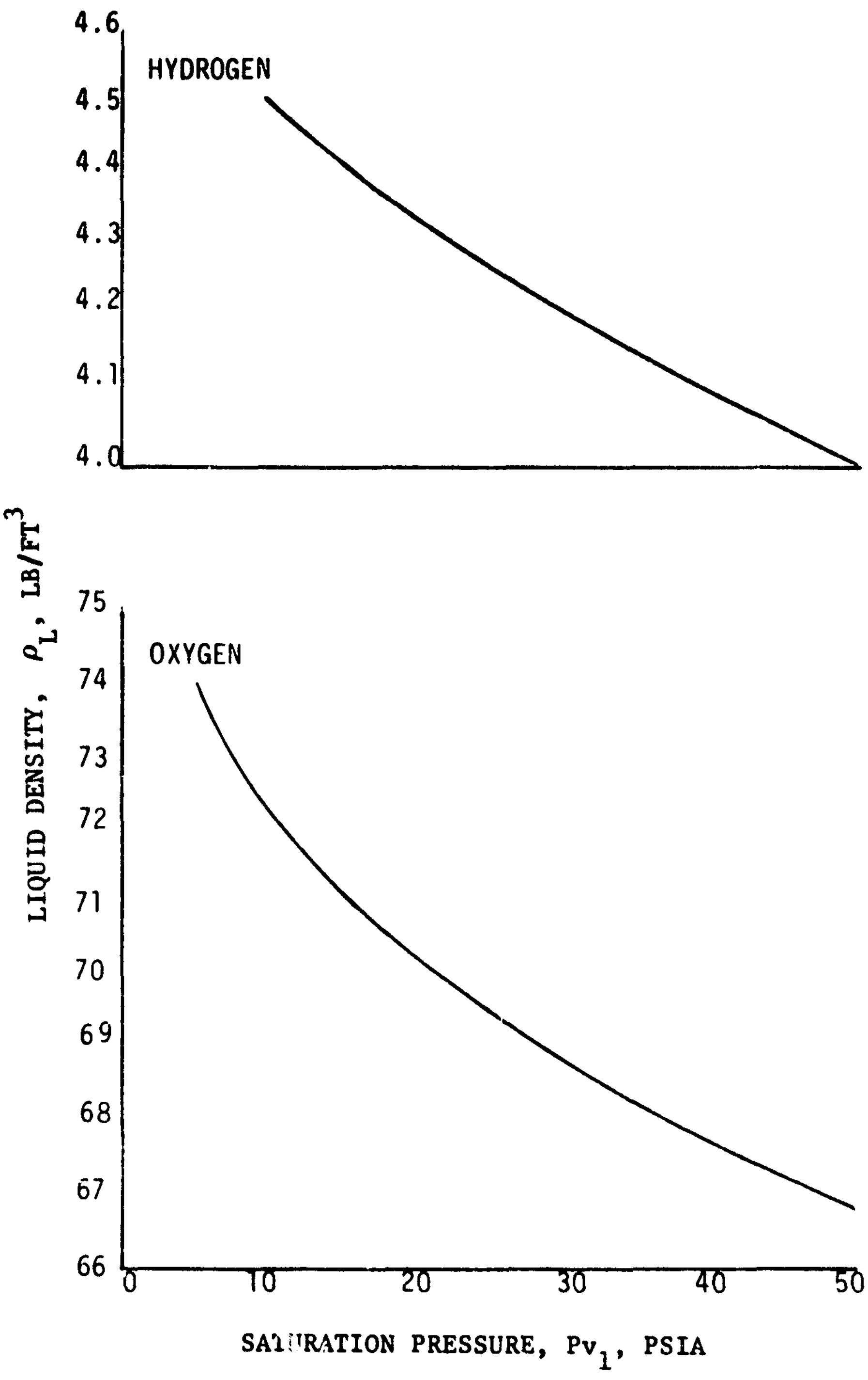


Figure 7-16. Liquid Flow Densities

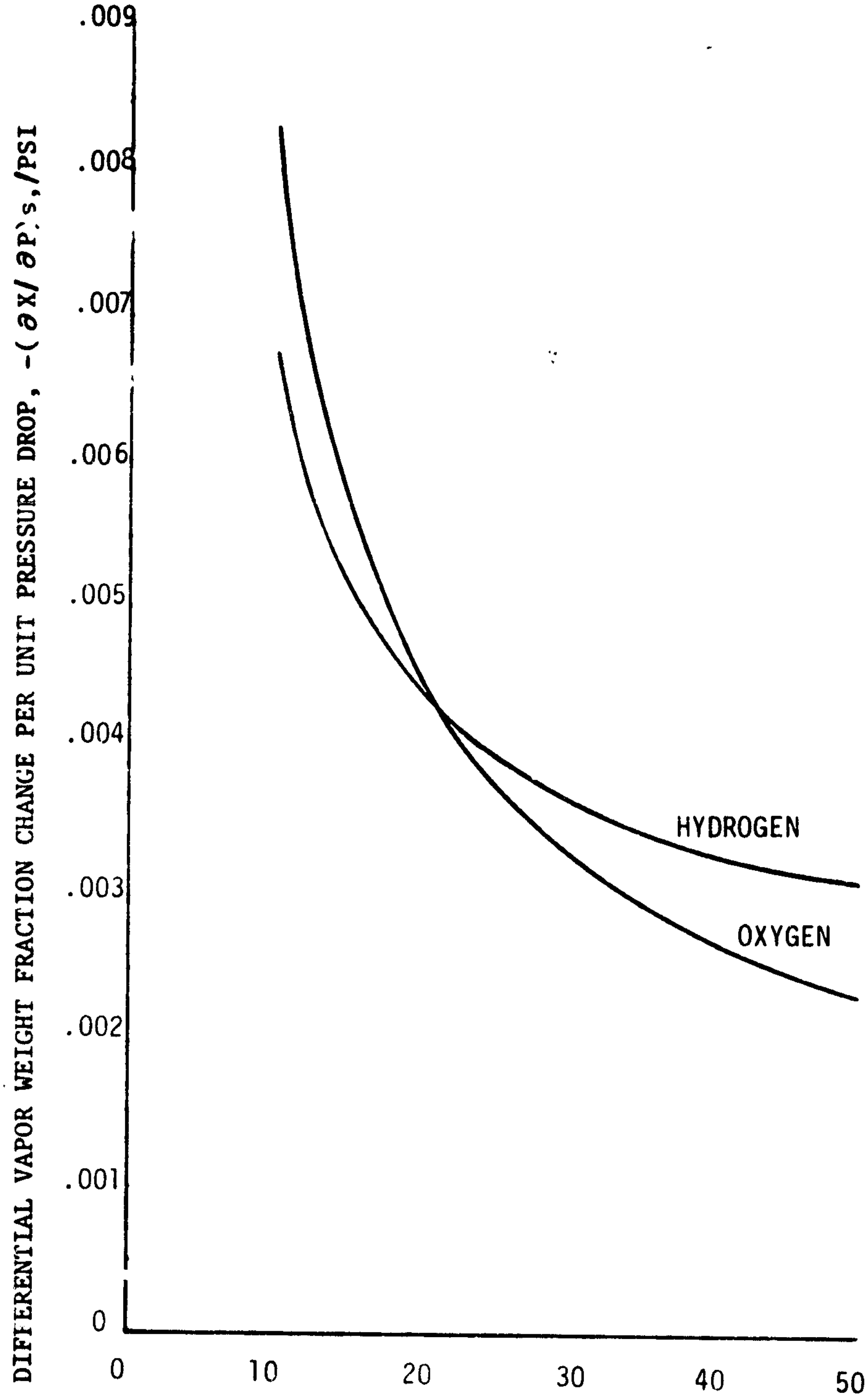


Figure 7-17. Vapor Fraction Differential Changes

For Isentropic Expansions from a Saturated Liquid

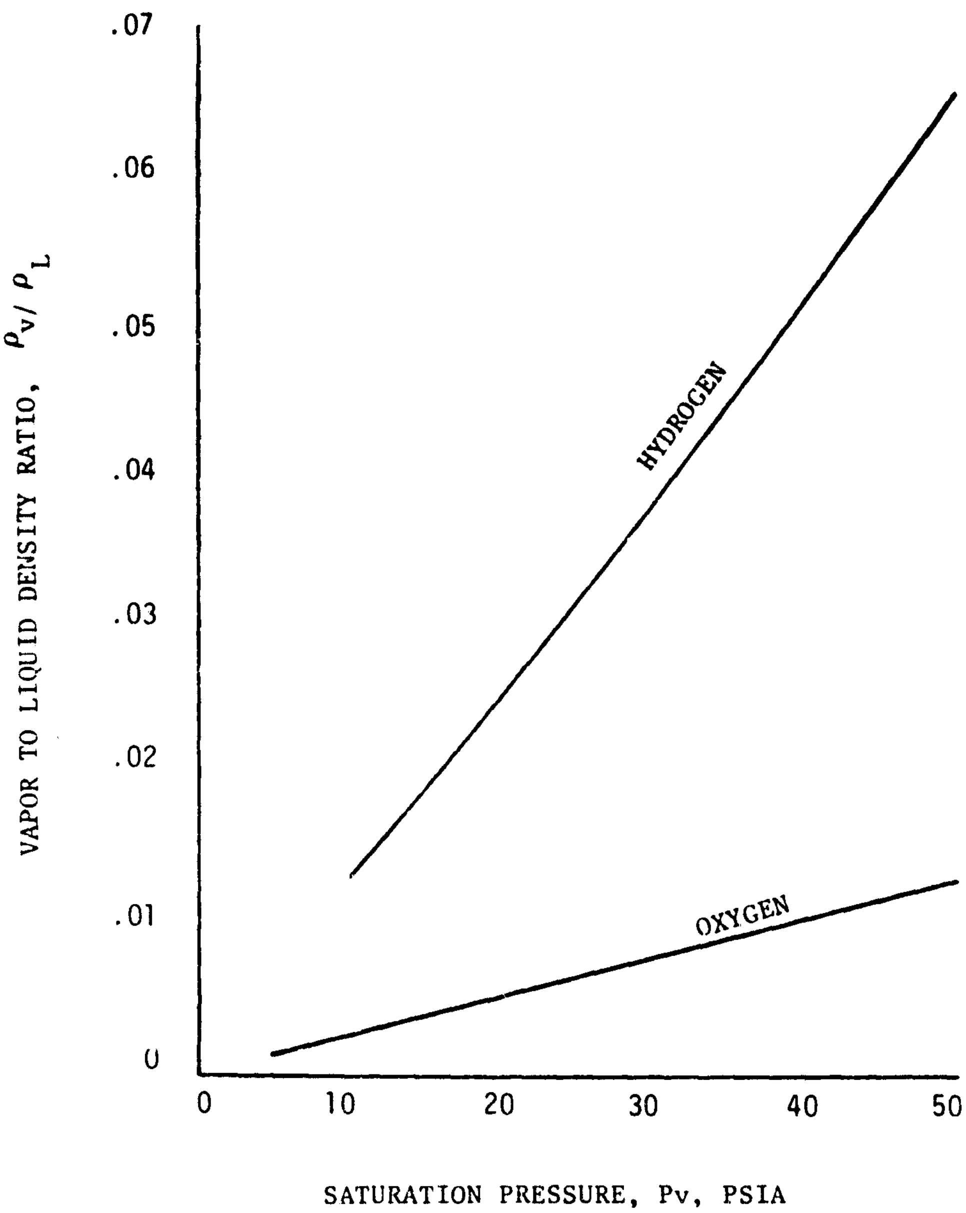


Figure 7-18. Vapor to Liquid Density Ratios

TABLE 7-31. OTV BOOST PUMP TWO PHASE* PUMPING EVALUATION UNDER FULL FLOW CONDITIONS

Fluid	Tank P Vapor, psia	Duct Loss Coef., K_L	Flow Accel. Time, Sec.	Vehicle Accel., G's	P Static, psia	Vapor Vol. Fraction, α, %		Flow Velocity, C_m, Ft/Sec	Flow Mach Number, M
						Supplied	Blade Capacity		
H_2	18.4	1.74	5	.1	17.85	9.9	27.5	22.6	.27
H_2	10	1.74	5	.1	9.47	21.5	29.8	25.2	.44
O_2	15.6	.67	5	.1	15.31	27.5	33.5	7.89	.82
O_2	10	.67	5	.1	9.72	46.1	34.7	10.43	1.23

* Tank NPSH = 0 (Sat. Liquid in Tank)

TABLE 7-32. OTV BOOST PUMP TWO PHASE* PUMPING EVALUATION UNDER PUMP IDLE MODE FLOW CONDITIONS

Fluid	Tank P Vapor, psia	Duct Loss Coef., K_L	Flow Accel. Time, Sec.	Vehicle Accel., G's	P Static psia	Vapor Vol. Fraction, α, %		Flow Velocity, C_m, Ft/Sec	Flow Mach Number, M
						Supplied	Blade Capacity		
H_2	18.4	1.74	5	.0121	18.38	.42	**	3.48	.047
H_2	10	1.74	5	.0121	9.98	1.05	**	3.38	.075
O_2	15.6	.67	5	.0121	15.61	0	38	.647	.00022
O_2	10	.67	5	.0121	10.01	0	39	.635	.00021

*Tank NPSH = 0 (Sat. Liquid in Tank)

**$(i/\beta)_L$ = .77, Which is beyond the angle of attack limit for two-phase flow

pump inlet conditions. As shown in Table 7-33 these limit indicating parameters are the inlet line Mach number under equilibrium flow conditions, the inducer blade vapor pumping capacity, and the inducer blade inlet liquid flow incidence to blade angle ratio.

The inlet line Mach numbers are the ratio of the flow velocity to the acoustic velocity (Equation 6).

EQUATION 6

$$M = \frac{C_m}{c} = \frac{\dot{w}}{\rho_L A \frac{\rho}{\rho_L} c} \approx \frac{\dot{w}}{\rho_L A\, c(1-\alpha)}$$

It is apparent that the product of the acoustic velocity (c) and one minus the vapor volume fraction $(1-\alpha)$ are required to make this calculation. Equation 7 is the expression for acoustic velocity in terms of flow densities, quality, and the acoustic velocities of the individual phases.

EQUATION 7

$$c = \frac{\rho_L / \rho}{\sqrt{\frac{1-x}{c_L^2} + \left(\frac{\rho_L}{\rho_v}\right)^2 \frac{x}{c_v^2} + \frac{\rho_L}{144g}\left(\frac{\rho_L}{\rho_v} - 1\right)\left[-\left(\partial x / \partial P\right)_s\right]}}$$

However, treating the vapor as a void fraction, and noting that the large single phase acoustic velocities and the low qualities cause the first two terms in the denominator to be small, Equation 7 reduces to:

EQUATION 8

$$c(1-\alpha) \approx \frac{1}{\sqrt{\frac{\rho_L}{144g}\left(\frac{\rho_L}{\rho_v} - 1\right)\left[-\left(\partial x / \partial P\right)_s\right]}}$$

TABLE 7-33. TWO PHASE PUMPING LIMITS

. INLET LINE CHOKING

EQUILIBRIUM MACH NO. < 1.0

. BLADE CHOKING

$$\alpha \leq \alpha_{MAX} = 1 - \frac{\phi_L}{\phi_{MAX}} = 1 - \frac{\phi_L}{.93\ \mathrm{TAN}(\beta_{1T} - 1.5)}$$

$$i_L / \beta_b \leq .7$$

This parameter utilizes fluid physical property data from Fig. 7-16, 7-17 and 7-18. The resulting values are shown in Fig. 7-19. Referring back to Equation 6, it is apparent that the ratio of the liquid flow velocity (the flow velocity if the flow were a pure liquid) to the acoustic parameter in Figure 7-19 is the approximate inlet line Mach number. Tables 7-31 & 7-32 indicate that this value exceeds 1.0 for only one condition, the oxygen line under full flow conditions at a tank saturation pressure of 10 psia. This indicates that the inlet line would be choked and, therefore, would have to be enlarged to accommodate the flow condition. The inlet line Mach numbers for the other 7 cases in Tables 7-31 & 7-32 are all less than 1.0 and, therefore, those inlet lines are sufficiently large to pass the flow.

The other major two-phase flow limit in Table 7-33 is the inducer blade choking limit which sets the inducer blade vapor pumping capacity. At the flow conditions existing at the leading edges of the inducer blades, very little area convergence is required to cause choking and, therefore, the inducer blade choking limit occurs (approximately) when the fluid angle of the two-phase flow is equal to the blade angle (References 7-6, 7-7 & 7-8). This is the limit because operation at a larger fluid angle (larger two-phase inlet flow coefficient) would result in an area convergence which, as stated earlier, would result in choking. As a result, the vapor volume fraction pumping capacity is proportional to the difference between the flow coefficient at the limit and the liquid flow coefficient (the flow coefficient if the entering flow were a pure liquid). With appropriate allowances for boundary layer and blade thickness, this is expressed by Equation 9.

EQUATION 9

$$\alpha_{MAX} = 1 - \frac{\phi_L}{.93\ \tan(\beta_{1T} - 1.5)}$$

Since the inlet tip blade angles for both OTV boost pumps are around 7°, this reduces to Equation 10.

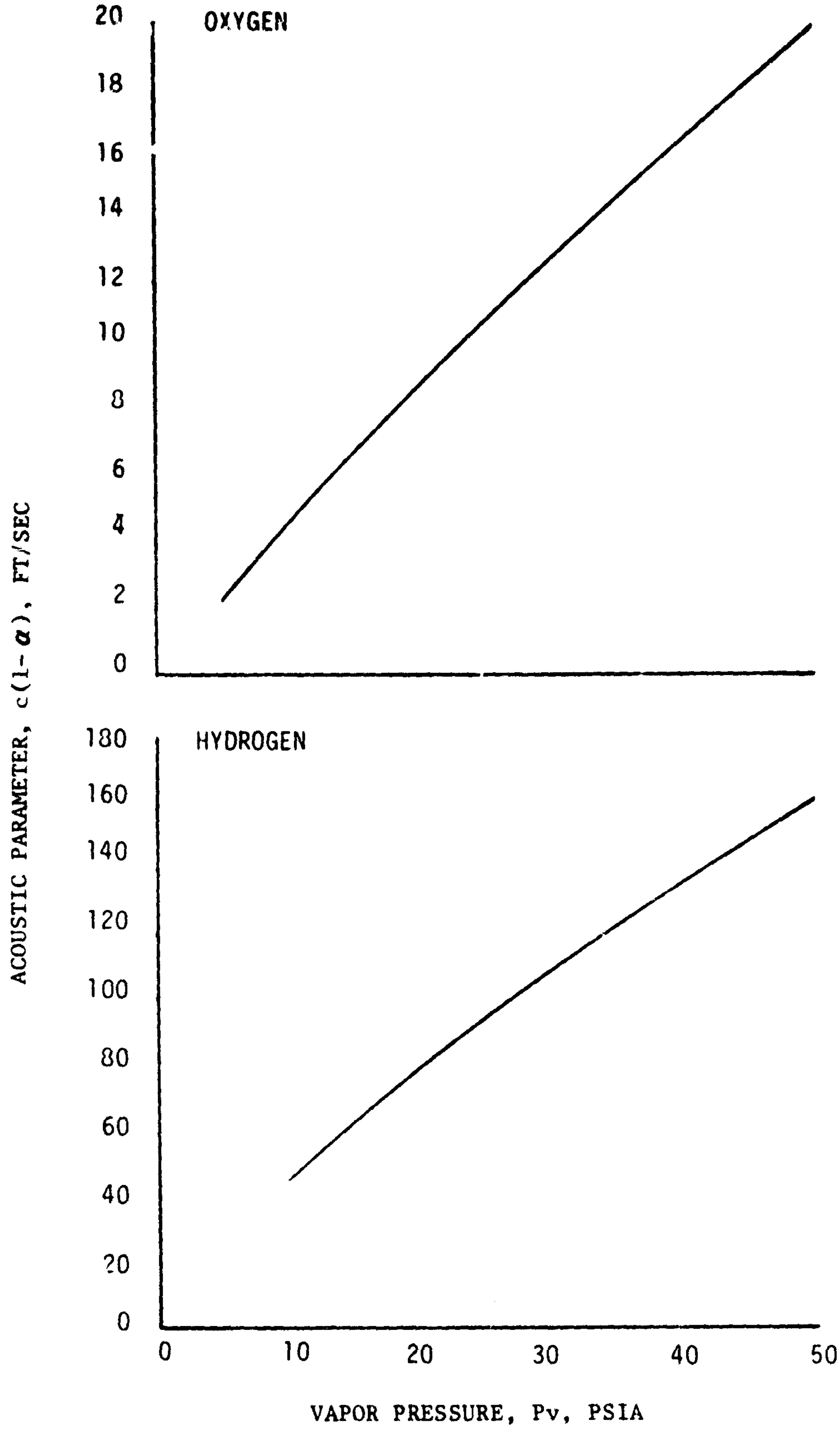


Figure 7-19. Approximate Acoustic Parameter

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EQUATION 10

$$\alpha_{MAX} = 1 - \frac{\phi_L}{.0895}$$

The upper limit to this vapor pumping capacity occurs when the ratio of the liquid angle of attack (the angle of attack, on the inducer blade leading edge tips, that the flow would have if it were a pure liquid) to the blade angle exceeds 0.7. Reference 7-8 indicated that vapor capacity becomes unpredictable at larger values. Equation 11 is the expression for this ratio.

EQUATION 11

$$(i/\beta)_L = \frac{\beta_{1T} - \text{Arc Tan}\left(\frac{\phi_L}{.93}\right)}{\beta_{1T}}$$

The vapor capacities of the OTV boost pumps, which are predicted by Equations 10 and 11, are shown in Fig. 7-20. It should be noted that some vapor capacity is probable at flow coefficients below .034 (beyond the $(i/\beta)_L$ limit); zero is indicated because the value is unpredictable and, under some conditions, appears to approach zero. These vapor capacities are compared with the supplied vapor fractions in Tables 7-31 and 7-32.

Evaluation of Results

The changes recommended to obtain zero tank NPSH operation with the OTV boost pumps are summarized in Table 7-34. They are discussed below at both the OTV tank vapor pressures (Table 7-36) and 10 psia tank vapor pressures.

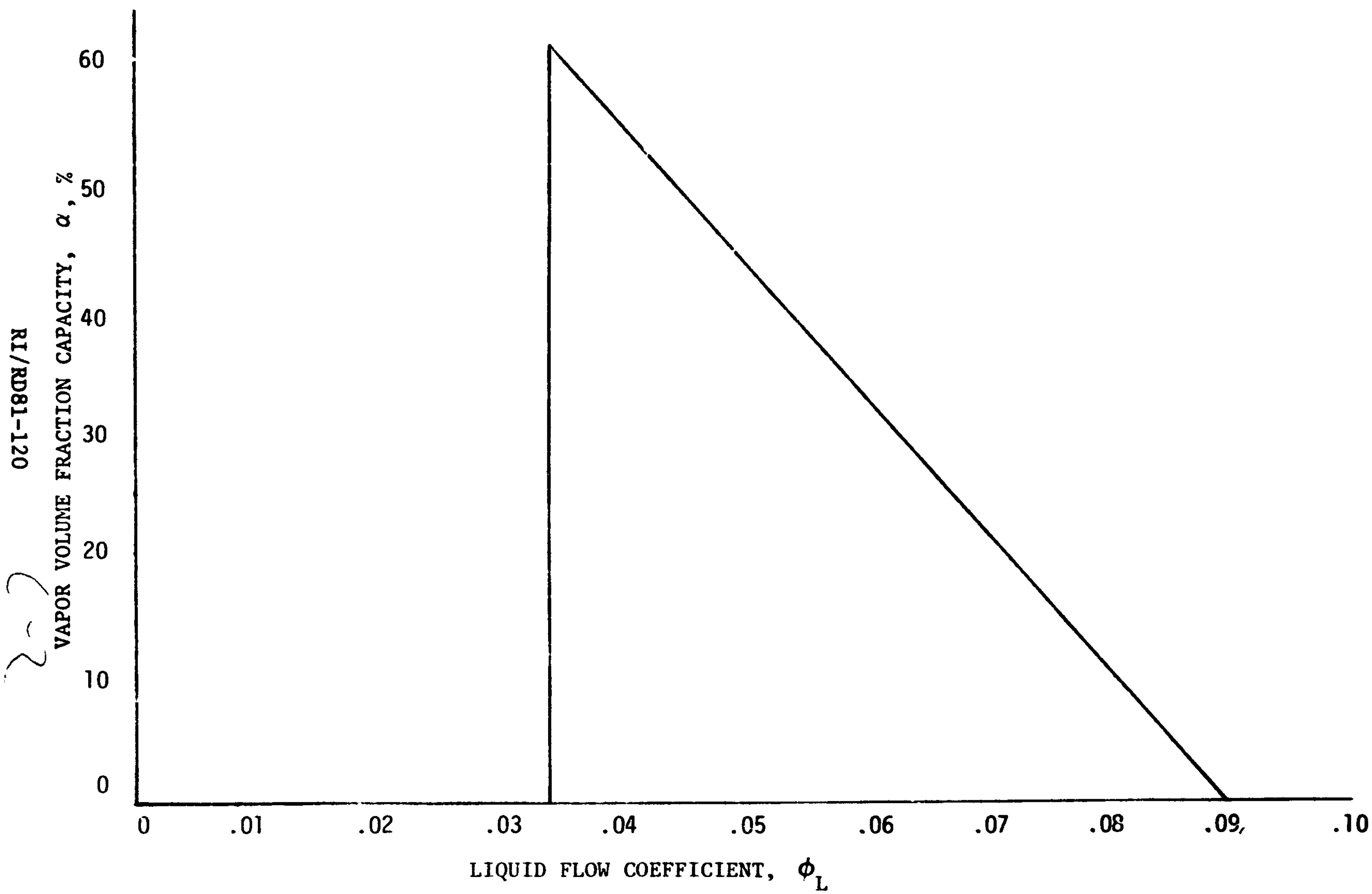

Figure 7-20. Inducer Blade Vapor Pumping Capacity for OTV Boost Pumps

C-2

TABLE 7-34. CONCLUSIONS AND RECOMMENDATIONS

Operating Mode	Tank Vapor Pressure, Psia	Recommended Changes to Boost Pumps	
		H_2	O_2
Full Thrust	Existing	None	None
	10 Psia	None	Increase Diameter 20%
Idle Mode	Existing	Decrease Speed 25%*	None
	10 Psia	Decrease Speed 25%*	None

*Probably Not Mandatory

OTV Tank Vapor Pressure. As discussed earlier, the inlet line Mach numbers at the OTV tank vapor pressures are all less than 1.0 (Tables 7-31 and 7-32) and, therefore, the presently anticipated inlet lines are sufficiently large to pass the flow. As also shown in Table 7-31, the vapor capacities exceed the supplied values under all full flow operating conditions at OTV vapor pressure. This is also true for the O_2 boost pump during idle mode (Table 7-32). However, the angle of attack on the hydrogen boost pump blades during idle mode is beyond the limit for two-phase flow. This is probably not a problem because the vapor volume fraction is less than 1%, and some vapor capacity is usually possible at angles of attack beyond the limit (Ref. 7-8). It may be concluded that the hydrogen boost pump speed during idle mode should be reduced 25% if it can be done easily without compromising other operating requirements. However, if difficulty is encountered, it would probably be satisfactory to leave the idle mode speed as it is.

10 Psia Tank Vapor Pressure. As shown in Table 7-31, the hydrogen inlet line for full flow operation is sufficiently large because the Mach number is at a very safely subsonic value of .44. However, this is not true for the oxygen inlet line because the approximation of inlet line Mach number exceeds 1, which means that the flow would be choked. Therefore, to avoid this with a safe margin, the oxygen inlet line diameter and, to be safe, the oxygen boost pump inlet tip diameter, should both be increased 20% in order to operate at full thrust at a tank saturation pressure of 10 psia.

As far as idle mode is concerned, Table 7-32 shows that the situation is almost identical to that at the OTV tank vapor pressures, i.e., the oxygen side is satisfactory and the hydrogen side is probably satisfactory but, to be safe, the speed should be reduced 25% if it can be done easily without seriously compromising something else.

APPENDIX A - REFERENCES

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